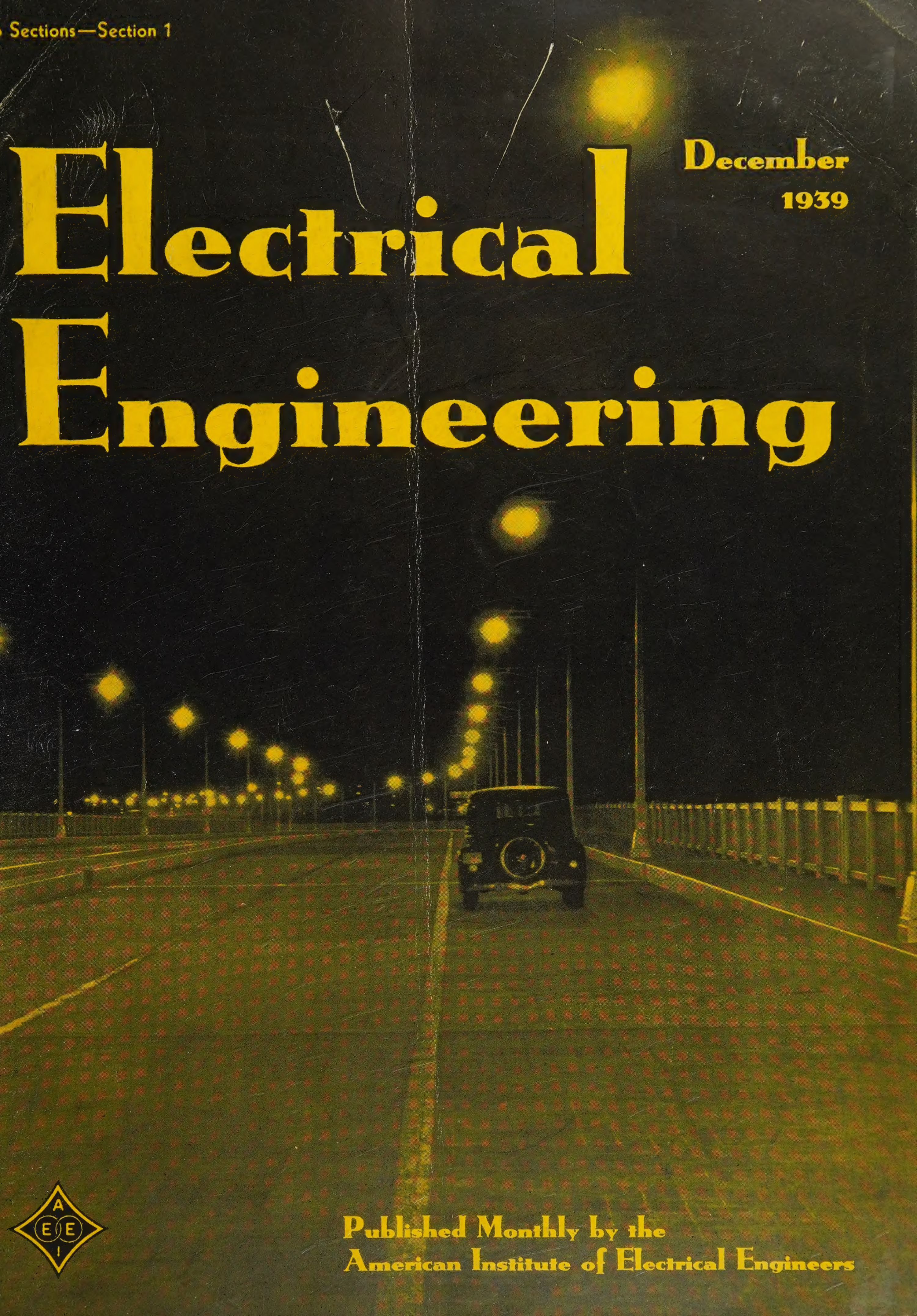
Sections—Section 1

Electrical Engineering

December 1939



Published Monthly by the
American Institute of Electrical Engineers

Electrical Engineering

Registered U. S. Patent Office

for December 1939—

The Cover: Sodium-vapor luminaires cast their golden brilliance over the new Main Avenue Bridge in Cleveland, Ohio. This is one of the many notable lighting installations mentioned in the current progress report of the AIEE committee on the production and application of light (see pages 497–508) Westinghouse Photo

Transactions Section (*Follows EE page 542; a preprint of pages 611–674 of the 1939 volume*)

VOLUME 58 NUMBER 12

Published Monthly by the

American Institute of Electrical Engineers

(Founded 1884)

Entered as second class matter at the Post Office, Easton, Pa., April 20, 1932, under the Act of Congress March 3, 1879. Accepted for mailing at special postage rates provided for in Section 1103, Act of October 3, 1917, authorized on August 3, 1918. ¶ Publication Office: 20th & Northampton Streets, Easton, Pa. Editorial and Advertising Offices at the headquarters of the Institute, 33 West 39th Street, New York

High Lights

Carrier System. A new 12-channel carrier telephone system for open-wire lines was first placed in commercial operation in Texas in 1938; various special problems arose in the application of the system to existing open-wire lines (*Transactions pages 666–74*). Increasing the transmission frequency range of the wire lines produced new problems with respect to attenuation, noise, and cross talk which were solved by modifications of construction, new transposition designs, improvement of impedance matches in parts of the circuit, and closer repeater spacings (*Transactions pages 656–65*).

Progress in Illumination. For many years, light sources and their application have undergone rapid changes as scientists and illuminating engineers sought improved and more efficient means of producing light and more effective ways of applying it. To provide a continuing record of that progress the AIEE committee on production and application of light has prepared at periodic intervals reports reviewing progress in this rapidly advancing field. The current report records the achievements of the past three years (*pages 497–508*).

Impedance Relays. High-speed impedance or reactance relays on a tie circuit between two systems will operate if the systems pull out of step, the indication being the same as though a fault existed; various blocking schemes have been devised to prevent such operation. In one recently applied, out-of-step blocking and selective tripping are obtained with existing relays and without the use of carrier current or pilot wires (*Transactions pages 637–46*).

Network Experience. The low-voltage a-c distribution network established in the central business district of the city of Philadelphia, Pa., some 12 years ago differs fundamentally from the more usual types of networks; it consists of a number of interlaced loop primary feeders, sectionalized by automatic oil circuit breakers, supplying a fused secondary network (*pages 517–21*).

Spectator Conveyer. During the 1939 season, more than 5,000,000 visitors to the General Motors "Futurama" at the New York World's Fair were transported through the exhibit on a specially designed conveyer consisting of a continuous chain of arm-chairs. An intricate sound system brought to each spectator a running account of the scene as it unfolded before him (*pages 509–14*).

Cable Loading. For a given load cycle and ambient temperature, ratings of cables installed in underground ducts will vary with the number of cables installed in the duct bank and its size. A study of economical loading has been made for cables in New York having a voltage range from 13.6 to 132 kv (*Transactions pages 611–18*).

Dielectric Strength. Porcelain suspension-insulator shells submerged in oil have been subjected to high-voltage breakdown tests to determine the impulse strength for both limited and repeated applications and the 60-cycle strength; ability to withstand stresses from steep-front lightning strokes is demonstrated (*Transactions pages 651–6*).

Condenser Bushings. Protection of the paper insulation in bushings of the condenser type from external influences is necessary in order to obtain long life. Improvements have been made in impregnating and varnish treatments, gasket materials, and weather-casing design (*Transactions pages 646–50*).

Human Science. It has been said that man has mastered the machine but has not yet mastered himself, which points to the need for more attention to the study of anthropology—the human science. In this issue, an AIEE member presents some thoughts on the subject (*pages 515–16*).

United Engineering Trustees. Officers were elected and annual reports were presented at the recent annual meeting of United Engineering Trustees, Inc., joint agency of the Founder Societies, by UET and its departments, The Engineering Foundation and Engineering Societies Library (*pages 530–4*).

Winter Convention. An address on atomic disintegration by Nobel Prize Winner Enrico Fermi will be a feature of the AIEE 1940 winter convention to be held in New York, N.Y., January 22–26. For the technical program, 19 technical sessions and 5 technical conferences are planned (*page 522*).

Ignitrons. The ignitron type of mercury-arc rectifier, which uses a high-resistance rod immersed in the mercury to initiate the arc, may be made in sizes large enough for railway service; voltage drop in the arc is less than that in the conventional rectifier (*Transactions pages 618–24*).

Ultrahigh-Speed Reclosing. Experience with re-energization of high-voltage transmission circuits in from 10 to 14 cycles after occurrence of a fault has justified the original expectation of a 75-per-cent reduction in outages on a 132-kv system (*Transactions pages 625–36*).

Corrections. TRANSACTIONS section, October 1939 issue, page 525, Lichtenberg discussion: Radio Manufacturers Association should be *Refrigeration Machinery Association;* American Cotton Manufacturers Association should be *Air Conditioning Manufacturers Association.* Contrary to the report published on page 483 of the November issue, the AIEE Student Branch of the University of Cincinnati *was* represented at the District conference of Branch officers held at Scranton, Pa., October 12, 1939—by Branch Chairman G. W. Little.

Coming Soon. Among special articles and technical papers now undergoing preparation for early publication are: a report prepared by the AIEE committee on power generation reviewing progress in that field during the past six years; an article reviewing a quarter-century of transcontinental telephony, by Past-President F. B. Jewett; an article discussing the various uses of electricity in chemical plants, by Kennard Pinder (M'37); a paper describing turbine-electric textile-range drives by E. L. Richardson; a paper discussing the characteristics and power requirements of spinning frames by E. A. Untersee; a paper on modern trends of low-voltage air circuit breakers by J. W. Seaman (A'32); a paper describing electrical equipment on machine tools by B. P. Graves; a paper on the measurement of carrier-current losses and minimization of interference on the Boulder Dam-Los Angeles transmission lines by J. D. Laughlin (A'36), W. E. Pakala (A'38), and M. E. Reagan (M'30); a paper discussing some factors in the mechanical design of high-speed turbogenerators by S. H. Mortensen (F'20) and James J. Ryan; a paper on special problems of two-pole turbine generators by C. M. Laffoon (M'39) and B. A. Rose; papers on hydrogen-cooled turbine generators by M. D. Ross (A'23) and C. C. Sterrett, and D. S. Snell (A'24); a paper on recent developments in telegraph switching by F. E. d'Humy (F'30) and H. L. Browne; a paper describing a high-gain d-c amplifier for bioelectric recording by Harold Goldberg (A'35); a paper describing the signal system, interlocking plants, and automatic train control on the San Francisco-Oakland Bay Bridge railway by C. R. Davis (A'36); a paper on electrical engineering and the petroleum refiner by G. R. Weeks, H. W. Giesecke (A'35), and C. M. Lathrop; and a report on progress of the art in electrical machinery, 1934–39, by an AIEE committee.

News 522

Subscriptions—$12 per year to United States, Mexico, Cuba, Porto Rico, Hawaii, Philippine Islands, Central and South America, Haiti, Spain, Spanish Colonies; $13 to Canada; $14 elsewhere. Single copy $1.50. ¶ Address changes must be received by the fifteenth of the month to be effective with the succeeding issue. Copies undelivered because of incorrect address cannot be replaced without charge. ¶ ELECTRICAL ENGINEERING is indexed annually by the Institute, weekly and monthly by *Engineering Index*, and monthly by *Industrial Arts Index;* abstracted monthly by *Science Abstracts* (London).

Number of copies this issue 23,100

Progress in the

Production and Application of Light

A. L. POWELL
FELLOW AIEE

For at least three decades, light sources and their application continuously have undergone rapid changes. To provide the Institute membership with a record of these developments, it has been the policy of the AIEE committee on the production and application of light to prepare at periodic intervals reports reviewing the progress achieved during those intervals. The 1939–40 committee* presents in this report a summary of the epoch-making advancements since 1936, when the last previous report was published,[1] prepared by a member and former chairman of the committee.

THERE have been numerous developments in the art of lighting during the past three years, but to the illuminating engineer this is nothing unusual. For at least three decades light sources and their application have always undergone rapid changes. It is true that the whole field of electrical engineering, being young and with a tremendous horizon, is in a constant state of flux, but those working in lighting have reason to feel particularly proud of the way that new frontiers have been attacked. To say the least, lighting is not static and those engaged in it are constantly experiencing the thrill that comes with change.

During the period under consideration there have been some epoch-making developments and numerous others that were foretold in the previous report. These take the nature of refinement and expansion of ideas promulgated several years ago.

In the last report it was pointed out that during the year 1935 more than 700 million incandescent lamps were sold, which was then a new high record. In 1936 this figure was exceeded, the total reaching 880 million. The year 1937 saw an all-time high of 955 million, and in 1938 there was a falling off to approximately 800 million; of this total, 60 per cent was what are called large and 40 per cent miniature lamps. The rate for 1939 to date indicates that this figure will be exceeded.

New Incandescent Lamps

Lamps for Photography

There probably has never been a period that has shown such a great growth in public interest in any hobby. Almost everyone is a camera enthusiast. The development of numerous types of inexpensive miniature cameras, of new types of film, of practical color film, and simple movie cameras has contributed to this end. Most of the amateur photographers are not content with natural light, but are riding their hobby for 24 hours a day. The so-called photoflash lamp has completely displaced the old magnesium flash bag, and the news and commercial photographers are requiring many forms of flash lamps. The new varieties of color film have presented lighting problems. The miniature camera has greatly increased the importance of enlarging. As a result of all this, the line of lamps designed especially for photography has grown by leaps and bounds.

The photoflood lamp series has been supplemented by three sizes, 250, 500, and 1,000 watts in blue bulbs for use with "regular" Kodachrome film. These also find application in indoor photography where artificial light is required to supplement daylight.

A 500-watt photoflood lamp with an inside aluminized reflecting surface is on the market and proves very convenient and compact.

Three years ago three sizes of photoflash lamps were listed which had bulbs loosely filled with very thin sheets of aluminum foil in an atmosphere of oxygen. This general form has been supplemented by lamps using extremely fine aluminum wire instead of foil, and the entire line has been expanded so that now no less than eight types and sizes of photoflash lamps designed for particular operating conditions are available. Space does not permit an analysis of details of their characteristics.

For amateur and professional photoenlarging there is now standardized a line of seven lamps in special white bulbs varying in size from a tiny *S*-11† bulb emitting 1,125 lumens to a *PS*-30 bulb emitting 15,800 lumens.

Since color photography requires light of a definite spectral composition for proper rendition, it has been necessary to make available lamps standardized to operate at a predetermined color temperature, namely, 3,200 degrees Kelvin. These, again, are of various forms and sizes (500 to 5,000 watts) and are 13 in number.

Certain classes of color photography require light sources at a still higher color temperature and five lamps (1,000 to 10,000 watts) are available that are designed to operate at 3,380 degrees Kelvin.

Lumiline Lamps

These tubular lamps with contacts at both ends are still quite popular where structural conditions make neces-

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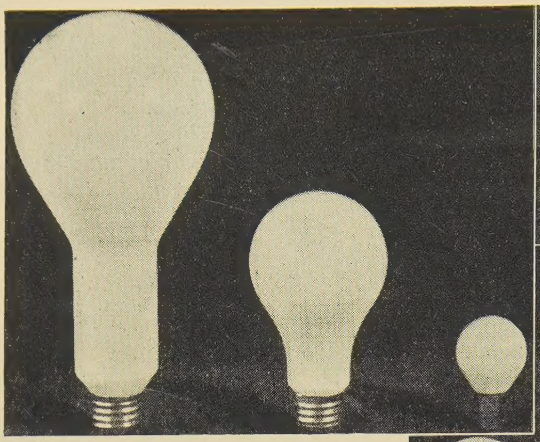
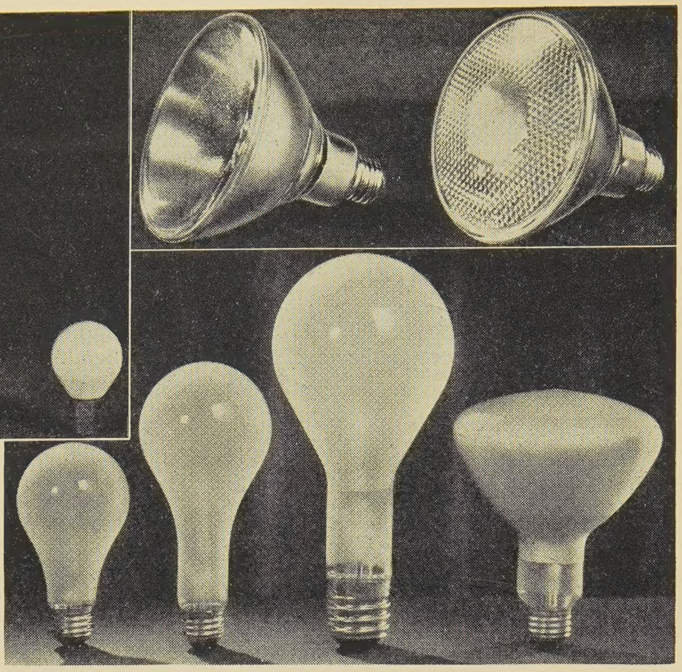
*Personnel of AIEE Committee on Production and Application of Light: D. W. Atwater, *chairman;* S. K. Barrett, Robin Beach, W. T. Blackwell, H. B. Dates, E. E. Dorting, C. L. Dows, L. R. Gamble, C. A. B. Halvorson, Jr., L. A. Hawkins, S. G. Hibben, W. C. Kalb, R. D. Mailey, P. S. Millar, G. T. Minasian, A. L. Powell, E. M. Strong, and I. A. Yost.

The author acknowledges the co-operation of various members of the committee in supplying data and checking the manuscript.

1. For all numbered references see list at end of report.

† In bulb designations the letter indicates the shape and the figure the maximum diameter in eighths of an inch.





Three sizes of photo-enlarging lamps, from the largest to the smallest (left group); photoflood lamps of the regular and reflector types (lower right group). At the upper right are (left) a typical spot lamp and (right) a typical flood lamp of the projector type

sary long narrow sources. There has been no addition to the standardized line, but the "white", "moonlight blue", "straw", and "orange" finishes are now made with inside rather than outside coloring, because of greater ability to withstand weather conditions.

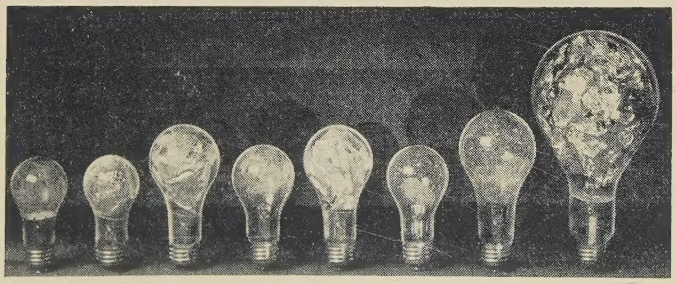
Three-Light Lamps

A considerable number of sizes of low-wattage medium-base three-light lamps have been offered for sale. In certain sizes the differences between the various steps are too small to be entirely practical and the combination of low-efficiency small filaments does not make a lamp that has much economic justification. Their novelty has given them a certain amount of popularity, however.

To meet the demand for a light source to provide higher levels of illumination from indirect portable luminaires in the home, a 200–300–500-watt *G*-40 bulb mogul-base three-light lamp has been made available.

Reflecting Processed Lamps

For several years rather large quantities of lamps with a silver coating on the hemispherical bowl of the bulb have been used for indirect lighting. The next step along this line was to coat the side walls of the bulb with reflecting material and use the lamp itself without external reflector to direct the light where needed. For this purpose, at first standard shapes of bulbs were employed, but these obviously were not designed to produce desirable distributions of light, so special filament mountings and bulbs of suitable contours were a logical step. Recently several new designs have appeared.



Eight sizes of standard photoflash lamps

Filaments of 150- and 300-watt ratings are mounted in bulbs 5 inches in diameter by $6^1/_2$ inches over-all with part of the inner surfaces given a special process mirrored finish of aluminum. These are available in two light distributions known as "spot" and "flood." The former is more concentrated and the latter of wider spread. This construction gives the following candle power readings at the peak of the beam:

300-watt spot	16,000	150-watt spot	7,000
300-watt flood	3,000	150-watt flood	1,200

The reflecting lamps just discussed have bulbs of ordinary glass and must be protected from the weather, but some new lamps known as projector lamps are made of heat-resisting glass. In these the bulb is made in two parts, the cover being fused to the bulb after the high-efficiency concentrated filament has been positioned. Two types of cover plates are used, one to give a flood and the other a spot distribution. In the 150-watt size the flood type gives a maximum candle power of 3,500 and the spot type 10,500.

Tubular-bulb lamps of various sizes with half of the bulb having a reflecting coating are available for showcases and other places where space is limited.

Drying Lamp

In many industrial processes radiant energy has been found to be most effective for drying purposes. Synthetic enamels and many lacquers can be dried in from 10 to 15 per cent of the time required by other methods. For this purpose a 250-watt *PS*-30 bulb tungsten-filament lamp has been developed. The filament is operated at an abnormally low temperature or efficiency ($7^1/_2$ lumens per watt), which avoids the production of unnecessary visible radiation and gives a very long lamp life. As a supplement there are on the market several types of concentrating reflectors for directing radiant energy to the surface

to be dried. Most of these are finished with electrolytic gold because this is efficient in reflecting the near infrared energy, permanent, and easily cleaned.

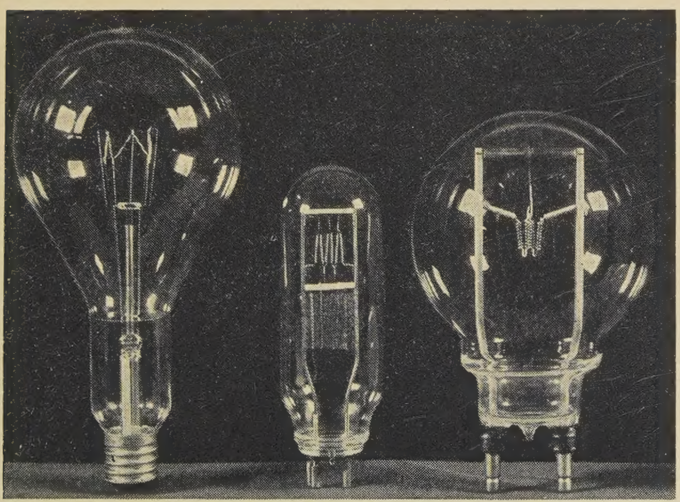
Projection Lamps

A rather startling advance has been made in this field with the development of 1,000- and 1,200-watt *T*-12 bulb biplane-filament lamps with a rated life of ten hours. The filaments and filling gas of these lamps incorporate improvements resulting in large increases in screen illumination and more uniform screen brightness. With the 1,000-watt lamp, for example, the lumen output is 32,000, and with a 16-millimeter motion-picture projector there is a gain of 50 per cent in light on the screen over lamps of previous design of the same wattage. In this field, also, improved optics have been introduced which make still further increases in screen brightness over that obtained with old style lenses and the same lamps.

With projecting lamps it is often necessary to prevent the light from escaping upward, and heretofore small metal caps have been used to slip over the ends of the tubular bulbs. Now the *T*-12 bulb 750-, 1,000-, and 1,200-watt projection lamps are regularly supplied with opaque end coatings.

An interesting medium-size projection lamp is the 300-watt *T*-$8^1/_2$ bulb lamp. This has a high-efficiency coiled-coil filament and a single-contact prefocused base. Being only $1^3/_8$ inches in diameter and $4^1/_8$ inches long it enables a source of 300 watts to be placed very close to the condensing lens and increases the amount of light entering the optical system of 8-millimeter motion-picture projectors.

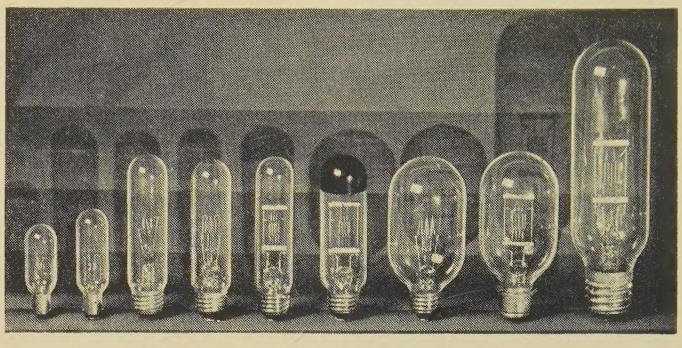
During the past two or three years a considerable number of quite inexpensive toy projectors have been placed on the market. These are far from optically perfect, but they require brilliant concentrated light sources of decidedly low cost. For this purpose there has been standardized a line of several sizes and types of toy-projector lamps which retail at very low figures..



Left to right: old style 1,000-watt PS52-bulb lamp; 1,000-watt T24-bulb medium bipost lamp; and a mogul bipost lamp for projection service

Bipost Base Lamps

The mogul bipost construction has become the accepted design for new types of high-current lamps. The medium bipost construction has been applied to the *T*-20 bulb 500-watt and the *T*-24 bulb 750- and 1,000-watt lamps for general lighting service, as well as to a considerable number of types of projection lamps.



A few of the projection lamps now available

The small over-all size made possible through the use of hard glass and the medium bipost construction has enabled designers to make commercial fixtures for high-wattage lamps of attractive proportions. A number of excellent designs along this line are on the market.

Important Changes in Standard Lamps for General Lighting Service

The standard life of 200- and 300-watt lamps has been reduced from 1,000 to 750 hours in line with production of light at minimum operating cost. The 300-watt lamp is now made in a shorter bulb with a medium screw base and 750-hour rated life. The 50- and 60-watt lamps are now standard in *A*-19 rather than *A*-21 bulbs.

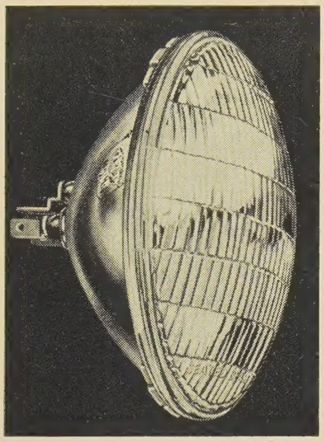
In the last report mention was made of the application of the coiled-coil filament to projection lamps. This construction makes possible increased efficiency in certain sizes of the regular lamps. The use of this coiled-coil principle has been extended first to the 60-watt size and here a 10-per cent increase in light output resulted. The uncoiled wire from such a lamp is 20 inches long, and with double coiling this is compressed to only $^5/_8$ inch. Since the filament is so concentrated the cooling effect of the filling gas is minimized. This construction was next applied to the 50-watt lamp, then to the 100-watt size, and a program is under way to expand its use as manufacturing facilities permit.

Flashlight Lamps

To permit accurate placement of flashlight filaments at the focus of reflectors, a simple type of prefocused base has been developed and applied to lamps of several sizes. Such accurate control, combined with die-cast reflectors, prevents distorted beams, provides more uniform and brighter spots of light, and materially increases the seeing range for general flashlight service.

SEALED AUTOMOBILE HEADLIGHTS

A most radical innovation in automobile headlights has taken place. In the past a typical assembly consisted of a plated parabolic spun reflector, an adjustable socket, a refracting cover glass or lens, and the necessary supports and housing. For accurate light control it was necessary to maintain three elements—the lamp, reflector, and lens—in exactly their proper relative positions. Even with rigid assemblies and careful workmanship, this was difficult because of the vibration and shock incidental to car operation. When these elements were out of optical adjustment a distorted and glaring beam resulted. In order to replace the lamp, it was necessary that the lens or cover glass be removable, and, in spite of attempts properly to gasket the joint, dirt and moisture accumulated on lamp bulb, reflecting surface, and cover plate. This accumulation materially reduced the light output.



"Sealed beam" automobile headlamp

Automobile manufacturers realize that with increased speeds safety in night driving required good headlighting and have been anxious to find some way of overcoming the two major difficulties just mentioned, produce something in which the human element of adjustment and maintenance was minimized, and obtain higher beam candle power. Work along this line has been proceeding for several years and has resulted in the development of two types of "sealed beam" headlamps, essentially the same in principle but different in structural details. These are entirely interchangeable, standard as to dimensions, and have the same light output and beam pattern. In one type there is a plated spun metal reflector to which a conventional-type two-filament bulb is soldered with the filaments in their correct positions. The lamp has three contact lugs so arranged that it cannot be inserted incorrectly in the receptacle. Across the mouth of the reflector is hermetically sealed a refracting-type cover glass. The maximum diameter is seven inches and the over-all length or thickness five inches.

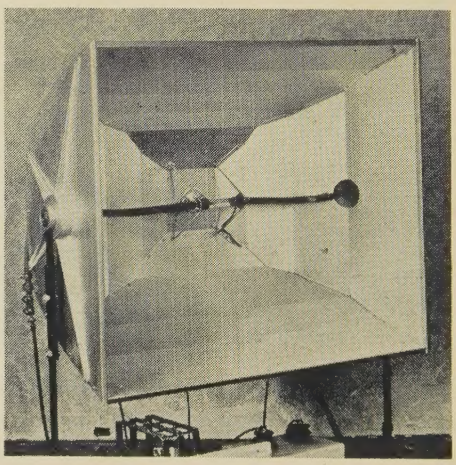
The other type of sealed-beam lamp is similar to the projector spot and flood lamps in general construction features. The lamp bulb itself (Mazda lamp number 4030) is the complete unit. The shape is known as the *PAR*-56. Essentially it consists of an accurately designed pressed hard-glass parabolic section with aluminized coating on the inner surface; a refracting cover plate is welded to the back half of the lamp. In it are mounted two 6–8-volt filaments: one taking 40 watts is positioned for the so-called country beam; the other consuming 30 watts is positioned to produce a lower and sidewise-directed traffic beam.

Practically all the 1940 model cars have this new sealed-beam headlamp. An ingenious mounting arrangement has been designed and a standardized and universal switching arrangement for the two beams will be found. Since the new country beam is more powerful than previous types of "high" beam, there will be a campaign of education to make sure that operators switch to the traffic beam when meeting oncoming cars. It is anticipated that this radical development will be a tremendous factor in safe night driving.

Gaseous-Conduction Lamps

Continued research and development has made possible quite a number of new types of these lamps. The high-intensity mercury-arc lamp line, of which three sizes were mentioned in the previous report, namely, 400-watt (type *A-H*1), 250-watt (type *A-H*2), and 85-watt (type *A-H*3), now has in addition a 100-watt lamp (type *A-H*4), which emits 3,500 lumens, has a rated life of 1,000 hours, is made in a *T*-10 bulb and provides a low-wattage small bright source. The 250-watt size is also made with a smaller arc stream. This is known as the type *A-H*5, emits 10,000 lumens, has a rated life of 1,000 hours, and is a medium-wattage small bright source useful for floodlighting, and other purposes. Details of the theory and construction of these new lamps have been published in ELECTRICAL ENGINEERING.[2,3]

The water-cooled lamp has now been placed in production and is finding numerous and interesting applications. It is designed to operate at 1,000 watts and emit 65,000 lumens. The lamp itself is a small quartz tube with an arc stream $1^1/_2$ millimeters wide by approximately one inch long. This tube is placed in an outer jacket through which a flow of water of approximately three quarts per minute is necessary. Some special jackets have been made in which three lamps are placed side by side in one container. A rather special control system is necessary to make sure that the lamp will not be lighted before the water is flowing properly, that the water will be turned off if the lamp or power fails, and, further, that no voltage is applied in case of lamp failure. This lamp, known as type *A-H*6, is proving most useful in photoengraving, contact printing, high-power enlarging, copy-board illu-



A photoengraving unit employing the 1,000-watt A-H6 water-cooled high-intensity mercury-arc lamp

mination, oscillograph lighting, and other fields where an extremely brilliant and concentrated light source is required.

In Europe the 400-watt hot-cathode mercury lamp with a certain amount of cadmium added is on the market, fitted with an exterior envelope or bulb, the inner surface of which is covered with fluorescent material. This combination produces a whiter light at no loss in efficiency.

The standard low-pressure mercury-vapor lamp (Cooper-Hewitt) has been provided with an improved circuit which enables the complete unit to produce the same light output with a 22 per cent reduction in power.

Fluorescent Lamps

The last report mentioned that phosphors were being applied to the high-voltage cold-cathode type of gaseous-conduction lamp and that work was under way toward the production of standardized replaceable lamps for operation on commercial voltages incorporating the fluorescent principle. This is now an accomplished fact and is one of the most important innovations in recent years.

Lamps are now standard in 18-, 24-, 36-, and 48-inch sizes, which have a uniform rating of 10 watts per foot. In brief, an arc stream of low current density passes through mercury vapor to which a small amount of argon has been added for ease in starting. A slight amount of visible radiation (three to four lumens per watt) of the characteristic mercury color is generated, but this is accompanied by a considerable amount of radiation at 2,537 angstrom units. This ultraviolet radiation impinges on phosphors coated on the inside of the tube and is transformed into visible radiation at surprisingly high efficiencies in most colors.

One of the most outstanding features of this development has been the production of new types of phosphors which have proved very effective. There are now standard phosphors radiating green, pink, and blue light. By mixing these a white light very closely approximating, 3,500-degree-Kelvin black-body radiation can be produced. Another mixture gives an artificial daylight good enough for most purposes equivalent to that of a black body at 6,500 degrees Kelvin. If the tube coated with pink phosphor has also a layer of red lacquer, through selective absorption a red light is produced. Similarly, if the white tube has a coating of yellow lacquer, a golden light results. Thus seven colors are on the market.

Efficiencies vary somewhat, depending on the tube dimensions and color, but are of the following order:

White	35 to 47 lumens per watt
Daylight	30 to 40 lumens per watt
Blue	21 to 26 lumens per watt
Green	60 to 75 lumens per watt
Pink	20 to 25 lumens per watt
Gold	25 to 31 lumens per watt
Red	3 to 4 lumens per watt

At each end of the tube is a small coiled coated electrode. In operation these electrodes are first heated and when the electron emission reaches the proper point, the arc is struck and the electrodes are de-energized.

Since these are essentially arc lamps, every lamp requires some device in series with it to act as a ballast. Choke coils and small capacitors are used for this purpose. The ballast and a switching arrangement for energizing and turning off the electrodes are combined in auxiliary devices. These are of several types and sizes and in certain forms the controls for two lamps are included in one casing. Work is still under way on auxiliaries, and each month or two sees the introduction of new designs.

The tube or lamp has a cap at each end on which are placed two small pin contacts across which the electrodes are connected. A special lampholder is required. Space does not permit a complete discussion of the operating characteristics, of the factors that influence lamp performance, and of the specialized applications of this new illuminant. Some of these features have been described in previous articles and papers.[4–6]

The principle of fluorescence has also been applied to the standard low-pressure (Cooper-Hewitt) mercury-arc lamp. Here a rectifying type of a-c operation is used and a high-efficiency fluorescent material coated on the inside of a 52-inch tube. The lamp and auxiliary consume 100 watts and produce 5,000 lumens. Although the color of the light is similar to that of the Cooper-Hewitt lamp, the fluorescence adds a considerable amount of red and color rendition is decidedly improved, although no claim is made for white or daylight color quality. This lamp finds particular use for high-level industrial illumination with units mounted relatively low.

Ultraviolet Sources

In the sunlamp class one lamp has been developed known as the *S*-4. This is the same as the 100-watt *A*-*H*4 lamp discussed under "Gaseous-Conduction Lamps" except that it is fitted with an *A*-21 bulb of special transmitting glass. It has a somewhat greater output of ultraviolet radiation in the anti-rickitic region than the *S*-1 lamp and consumes only one-quarter the energy. The fixtures for the *S*-4 sunlamp are smaller, lighter, and easier to handle.

In the germicidal type, the "Sterilamp,"[7] a high-voltage cold-cathode type, is now made in 10-, 20-, and 30-inch sizes with ratings from about 10 to 20 watts. The following additional units have been made available:

Watts	Bulb	Base	Necessary Auxiliary	Life (Hours)	Microwatts Per Sq Cm of 2,537 A Radiation at One Meter
3	*T*-4	Radio type	Separate reactor	1,000	3–4
5	*T*-10	Medium screw	None required	1,500	1
15	*T*-8	Fluorescent	Same as for 15-watt fluorescent lamp	1,500	15–20
5	*T*-6½	Candelabra screw	Separate resistor	4,000	0.5
16	*T*-4	2-pin special	Separate reactor	1,500	15–20

These various germicidal tubes are finding considerable application for destroying bacteria. They have been experimentally installed in air ducts to kill air-borne bacteria; they are being used as a preventative to the

spread of disease in hospitals, and for food preservation by various industries. Sanitization of drinking glasses and sterile storage of previously sterilized utensils are applications for these lamps which are becoming more popular every day. The relatively small size of some of the new tubes and simple control devices make them suitable for installation in places where space is limited.

A very useful new source for producing ultraviolet radiation for fluorescent and phosphorescent purposes is a result of modification and adaptation of the 100-watt type *A-H*4 lamp. This is made with a *T*-16 bulb of natural red-purple glass and emits a considerable amount of radiation of the type required for producing fluorescence, at the same time screening out most of the visible radiation. It is known as the type *B-H*4 lamp.

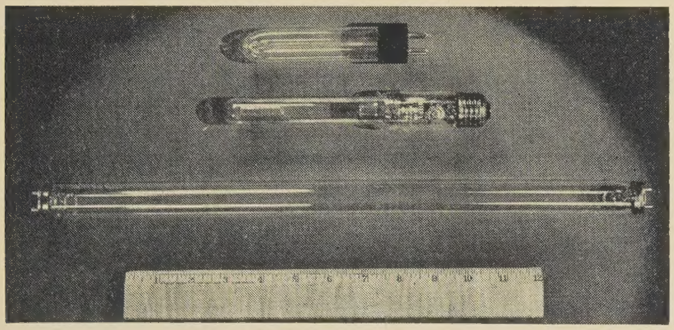
The Uviarc, a powerful source of ultraviolet radiation for industrial purposes, has been adapted for a-c operation. An entirely new line of these lamps with oxide electrodes and a limited amount of mercury has been introduced.

Carbon Arcs

An improvement in carbon arc lighting for the motion-picture studios has been effected since the previous report of this committee through the development of a broadside lamp with motor-driven carbon-feeding mechanism. This development gives improved steadiness of light and a much lower sound level than lamps with solenoid-operated feed mechanisms.

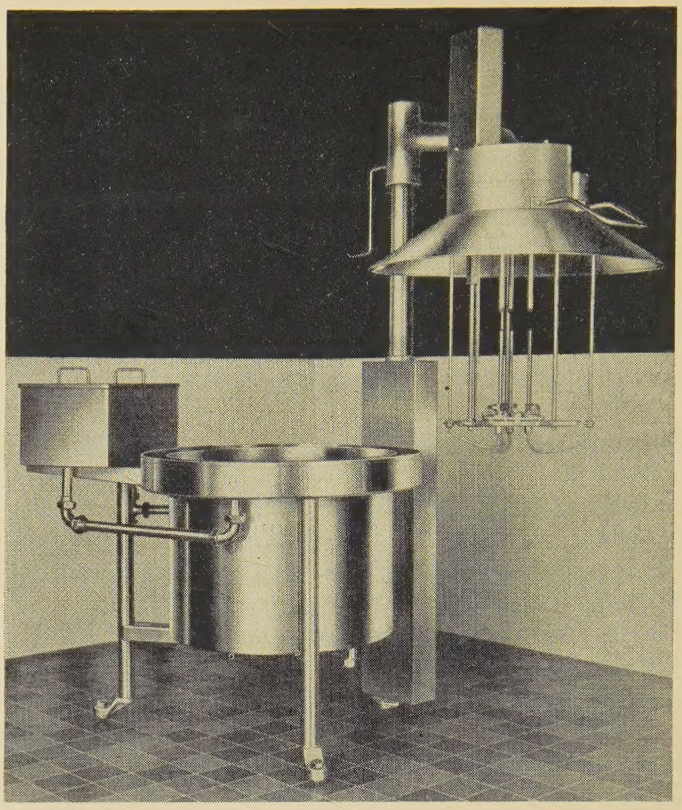
A new super high-intensity carbon, 11 millimeters in diameter, provides an intrinsic brilliancy of 1,200 candles per square millimeter at an arc current of 135 amperes.

The development of carbon arc lamps for 16-millimeter motion-picture projection is extending the scope of the 16-millimeter film in educational and commercial fields, permitting the projection of a brilliant, sharply defined image on a much larger screen than has heretofore been practicable.



Three ultraviolet lamps of the germicidal type

The 3-phase carbon arc has been adapted to modern requirements and applied to a number of industrial uses. A 12-kw lamp, burning six carbons on a wye circuit, is being used for the vitamin-*D* activation of milk. For purposes of illumination this lamp provides a light source of one million lumens. A one-kilowatt, three-carbon delta-connected lamp is used in a new accelerated-fading-test unit. This lamp, equipped with carbons giving a spectrum similar to sunlight, is also used for illumination on applications requiring high-intensity light of daylight quality, free from the stroboscopic effects sometimes experienced with the single-phase arc.



A 12-kw arc lamp burning six carbons on a wye circuit being used for vitamin-D activation of milk

New Materials for Lighting

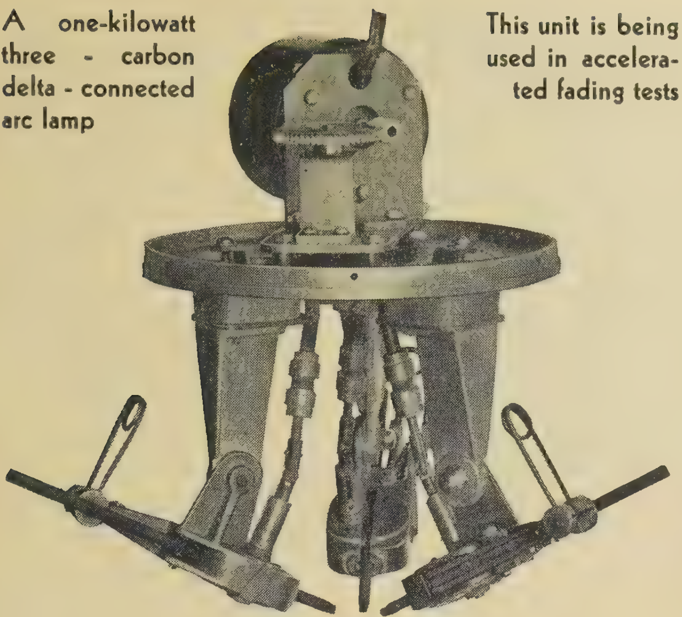
Glass blocks or bricks as structural elements have seen somewhat more extensive use during the past few years. Since these are translucent they offer opportunities for novel lighting effects, and quite a number of colorful schemes have been applied.

A new form of tempered glass has been introduced, and to this enamel of various colors is applied. This material is suitable for special effects in luminous store fronts and the like. In the same field different forms of weather-resisting metals lend themselves to interesting design in co-ordination with light and color. Mat and polished finishes, as well as porcelain-enameled corrugated metal are available to the designer.

Under normal conditions it is difficult to control the light reflected from a ceiling, for with plaster, sound-proofing materials, and other coverings diffuse reflections result. One manufacturer of metal ceilings has placed on the market sheets so formed that controlled or regular reflection results. This is known under the trade name of Parab-O-Lume.

A colorless transparent plastic known as Lucite has found certain interesting applications. This material has the property of retaining a beam of light through in-

A one-kilowatt three - carbon delta - connected arc lamp



This unit is being used in accelerated fading tests

ternal reflections in much the same manner as a quartz rod. It has been applied to curved dental and surgical lighting devices where the beam is bent around the corner to illuminate some cavity.

Notable Lighting Installations

During the period under consideration there has been relatively little new building, but considerable revamping and modernization of existing structures. Practically all the leading shops on Fifth Avenue, New York, State Street, Chicago, and similar merchandising centers have installed unique and effective up-to-date lighting within the last few years. Many public rooms in hotels, restaurants, night clubs, and cafes are completely changed in appearance, and some of the most ingenious lighting to be found anywhere may be noted in this class of service.

It is impossible to tabulate the number of larger department stores that have discarded old systems and installed illumination schemes to take advantage of the most recent developments. As representative of this field we might mention: Gimbels, New York, N. Y.; Marshall Field, Chicago, Ill.; Kauffman's, Pittsburgh, Pa.; J. L. Hudson, Detroit, Mich.; Orecks, Duluth, Minn.; Z. C. M. I., Salt Lake City, Utah; Saks, Los Angeles, Calif.; Roos Brothers, San Francisco, Calif.; T. Eaton Company, Toronto, Ontario, Canada.

Remarkable advances have been made in illumination of banking institutions, and both the Los Angeles and Toronto stock exchanges have been completely relighted. The new service building of the Detroit Edison Company is an example of the application of many new principles of illumination, and the main office building of the Commonwealth Edison Company has an entirely new system.

Details of new and novel lighting installations are discussed in each issue of the Illuminating Engineering Society *Transactions* under a section conducted by the society's committee on the co-ordination of light and architecture. Those desiring to keep in touch with the most modern practice should refer to that publication.

There have been a number of international expositions or world's fairs, and, of course, in these the latest developments in lighting are always incorporated. Details of the Texas Centennial,[8] Great Lakes,[9] Paris,[10] and Golden Gate,[11] expositions have been reported in ELECTRICAL ENGINEERING and in other publications.

A manuscript describing and giving technical details regarding the lighting installation at the New York World's Fair 1939 is under preparation and is scheduled for publication in ELECTRICAL ENGINEERING when completed.

Lighting Instruments

Not long ago the rather cumbersome bar photometer with grease-spot screen was the only light-measuring device. With the development of the art of illumination the need for additional measuring instruments has grown apace, and each period has seen new types made available.

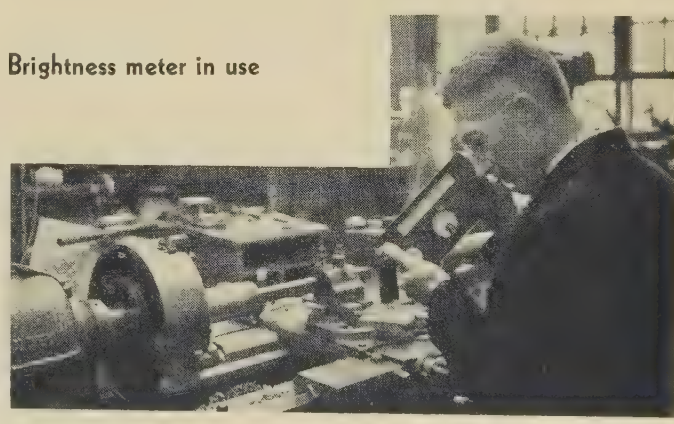
One rather interesting device, first developed in England, has been introduced in the United States, known as a light selection recorder. The observer, or subject, has a means of varying the illumination level on a test object and eventually selecting the amount of illumination he prefers. At this point he presses a button and on a printed scale is indicated the result of his choice.

With the growing appreciation of the importance of brightness measurements, some special meters have been developed for this purpose. A rather expensive and accurate type is known as the Luckiesh-Taylor brightness meter. A less expensive and more simple instrument, known as a model *L-H*, was developed by Luckiesh and Holladay.

Baumgartner has developed a light-cell reflectometer[12] and a light-cell distribution photometer. This latter device enables one to obtain a candle-power distribution curve in an extremely small fraction of the time required by the older method.

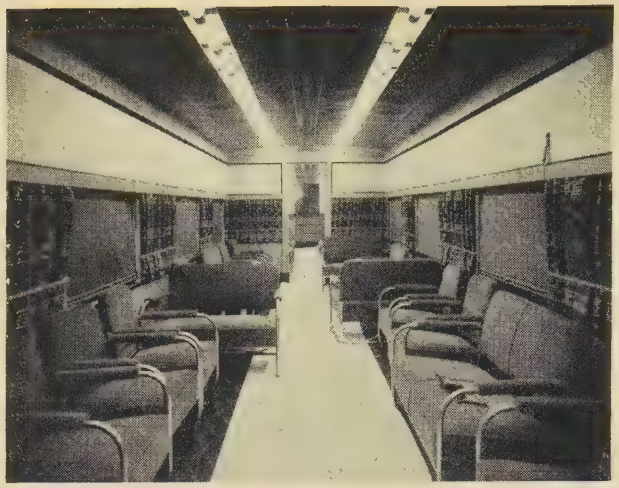
The widespread use of photoflash lamps has made it necessary to find some means of checking the setting of camera shutters in relation to the peak of light output. For this purpose a rather simple device known as the "synchrograph" was introduced. Another instrument along this line is known as a "flashograph," which traces

Brightness meter in use



on a moving film the performance of photoflash lamps. The lamp is flashed in a small sphere which has a photoelectric cell mounted on it. The amplified output of the cell is applied across a small Rochelle-salt crystal oscilloscope. The deformation of the crystal in reflecting a beam of light across the aperture of a drum traces the candlepower-time relation on film.

With the development of the ultraviolet lamp of the germicidal type (Sterilamp) the need arose for an instrument that would provide an exact quantitative measurement of the abiotic radiation from these lamps. The instrument developed to meet this need is an ultraviolet meter embodying a tantalum photoelectric cell, which has a wave-length response to ultraviolet radiation similar to the lethal action of these radiations upon bacteria. With the aid of this device, it has been possible to determine the abiotic energy necessary to kill any chosen micro-organism, and sterilizing installations may be planned with an exactness approaching that of the normal lighting installation.



Interior of the "John Wilkes," express train of the Lehigh Valley Railroad, the first to be lighted by fluorescent lamps throughout

Transportation Lighting

Real progress has been made in this field. Some of the tendencies have previously been reported.[13] New cars and busses, in general, are quite up-to-date from the lighting standpoint, and much work has been done in revamping and modernizing old vehicles.

With the coming of the fluorescent lamp the railroad engineers were among the first to consider its use. Since these lamps require alternating current for most efficient operation, the first step was the development of a vibrator inverter for operation on the storage-battery circuits. This device has worked out well in service. In some cases a small motor alternator set is used. The New York Central Railroad was the first to put in operation a fluorescent-lighted car. The Lehigh Valley Railroad's remodeled express train known as the "John Wilkes" has the honor of being first lighted by fluorescent lamps throughout.

Light in Horticulture

There is considerable interest on the part of various agricultural experimental stations in growing plants entirely under artificial light for the purpose of eliminating variables in order to study the effect of different chemicals, both for nutrient solutions and soil growth.

At Cornell University, Ithaca, N. Y., there is a room equipped with 24 1,000-watt lamps and a water filter, as well as humidity control. Ohio State University, Columbus, has a similar installation and is now experimenting with daylight fluorescent lamps producing 1,500 foot-candles for studying the growth of tomato plants.

The Boyce Thompson Institute for Plant Research, Yonkers, N. Y., has carried on continuous work and has recently conducted a series of tests comparing the effectiveness of various fluorescent lamps with other sources for plant growth. The University of California, Berkeley, is doing the same type of work and had a rather impressive exhibit at the Golden Gate Exposition during the 1939 season.

The color value of the daylight fluorescent lamp is apparently more suitable than that of filament lamps for stimulating plant growth. It gives much less radiant heat per foot-candle, and there is every indication that it will be an important factor in this field.

A notable installation of carbon-arc lamps for stimulating plant growth has been made at the field station of the United States Department of Agriculture, Beltsville, Md.

Sports Lighting

All the American Association baseball fields are now lighted for night playing and the "big" leagues are gradually recognizing the fact that night games pay well. Ebbetts Field in Brooklyn, N. Y., has installed a total of 615 1,500-watt incandescent floodlights, and the Cleveland Municipal Stadium has now 712 units of the same size.

Show-Window Lighting

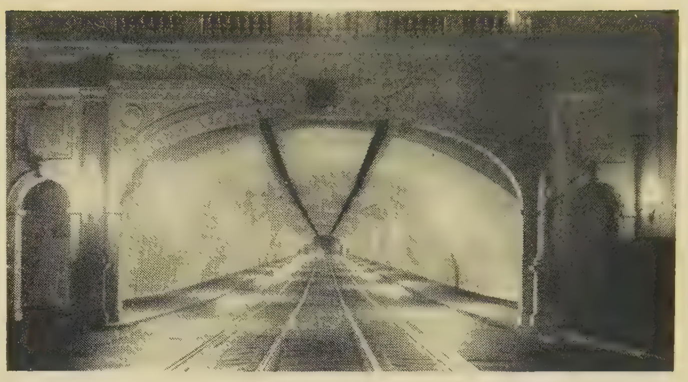
More than two decades ago the idea was promulgated that the show window was a miniature stage and that all the effects used on the latter would find application for the window display. It finally appears that the more progressive merchants are appreciating the logic of this approach. In the more artistic type of window spot lamps equipped with color mediums, flood effects, colored illumination of background, and combinations of all these are being applied with splendid results.

Street and Highway Lighting

Each year shows a growing public appreciation of the advantage of highway safety lighting even if one considers only the dollars and cents involved. New tests constantly show that the cost of this lighting is soon offset by decrease in accidents. In several states modernization plans are under way; in certain instances the installations are owned by the state. Many of the important cities have shown marked improvement.

There have been a number of new forms of street luminaires, most of which incorporate efficient Alzak reflectors. A rather novel feature in one type is the reflector spun onto the globe providing a weatherproof seal and greatly reducing glassware breakage.

Sodium lighting with its distinctive color has proved useful as a caution signal for safety-island beacons, as well as for especially dangerous intersections. The two new large bridges in California, the Golden Gate and the San Francisco-Oakland, as well as the Blue Water Bridge over the Saint Clair River between Michigan and Canada and the new Main Avenue Bridge in Cleveland, Ohio, are all lighted by the sodium system. This efficient light has also proved very acceptable for underpass and tunnel lighting, notable installations being the Two Rock Tunnel on the Columbia River Highway in Washington, the Liberty Tunnel in Pittsburgh, Pa., the Stockton Street Tunnel, San Francisco, and the McCallie Avenue Tunnel, Chattanooga, Tenn.



Stockton Street Tunnel, San Francisco, Calif., lighted by 10,000-lumen sodium-vapor units

Aviation Lighting

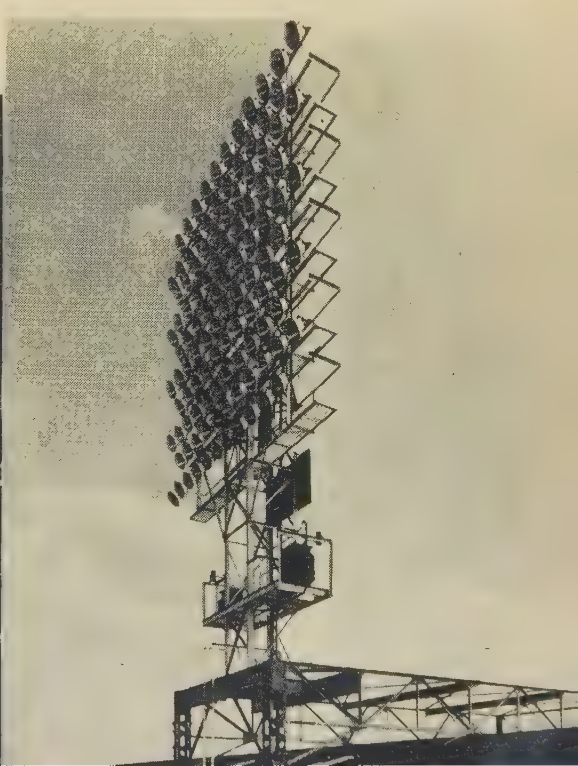
Improved efficiency in the design of conventional types of aviation lighting equipment and the introduction of new types of lights have combined to make the past four years a period of important progress. The trend in beacons has been a return to the 24-inch size but the design has been "streamlined." The trunnion arms have been eliminated, reducing the area exposed to wind and the risk that ice and snow may interfere with smooth rotation. The base has been redesigned to afford better access to the mechanism for maintenance. A new lamp changer eliminates the shadow of the reserve lamp from the beam. Another form of this beacon is also available in which the drum has been replaced by a glass dome, the rotating parts thus being completely enclosed. To provide a beacon with more frequent flashes, an oscillating beacon has been developed. In this unit a lamp moves up and down on the axis of a cylindrical Fresnel lens giving 80 flashes per minute. This beacon also has more candle power at high angles than the 24- or 36-inch beacons.

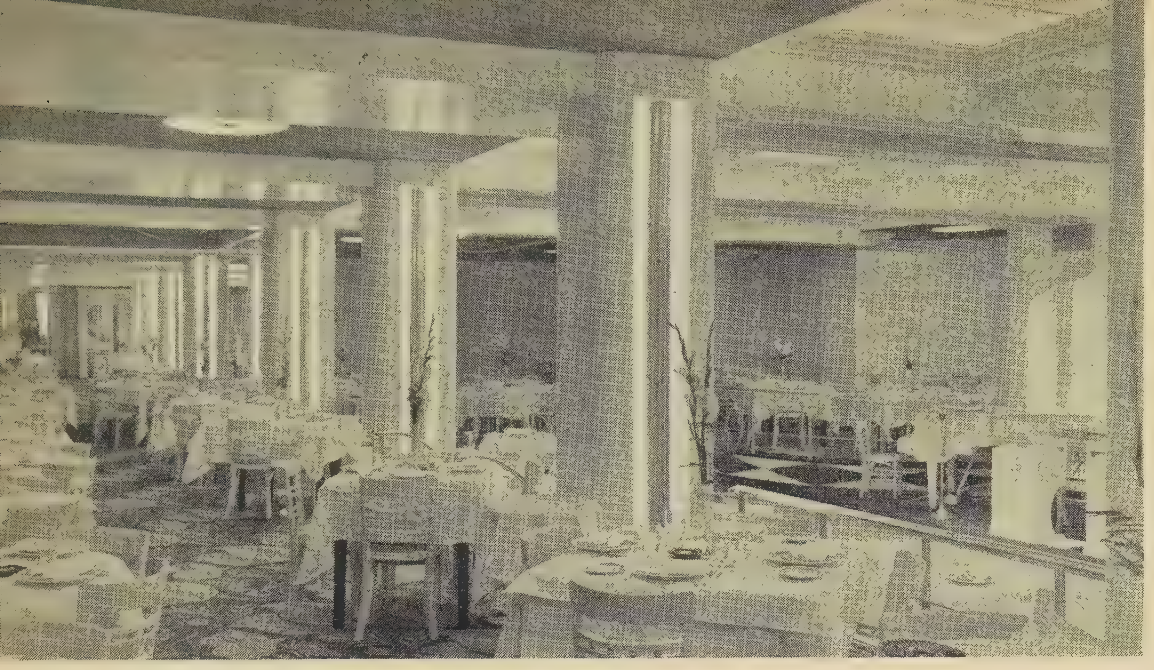
An experimental boundary-light circuit in which the lamps are fed by a 550-volt multiple circuit through individual transformers has been installed at Washington (D. C.) Airport. The circuit is less expensive than a series circuit and the voltage drop is much less than with an ordinary multiple circuit. It is well adapted for use on fields that do not have sufficient night traffic to warrant the outlay required for a series circuit.

Floodlighting is being designed with greater attention to the avoidance of glare. This has led to the development of an improved 90-degree unit employing four 1,500-watt lamps each in a separate Fresnel lens. The four separate sources give insurance against a sudden interruption of the illumination by the burning out of a lamp.

The development of the approach-and-contact-light system has largely taken place in the last four years. This system was originally designed to supplement radio aids in fog, but its use in good weather has found much favor. The approach lights are located outside the landing area and are accordingly red. Neon lamps approxi-

Shibe Park, home field of the Philadelphia (Pa.) baseball club of the American League, illuminated by 780 1,500-watt floodlights mounted high above the field on eight towers, one of which is shown at the right





Grill room of a New York, N. Y., hotel with fluorescent units recessed in the columns

mately six feet in length, with suitable parabolic reflectors, give an intensity of 1,000 candles and, by their elongated shape, establish a horizontal plane of reference to guide pilots in approaching the runway. The contact lights are arranged along the two sides of a runway to show its location and indicate the ground level. These units project less than two inches above the runway and are available in two types. Heavy-duty units are used on runways that are in line with approach-light systems or furnished with radio aids for instrument landings. Normal-duty units are used on other runways. Contact lights are lighted on only one runway at a time. The approach-and-contact-light system, in addition to being one of the most satisfactory aids for night landings, is also the most effective means available for giving an airport a distinctive appearance at night; this is a great aid to a pilot looking for an airport with which he is not familiar.

There has been marked progress in the development of traffic-control lights. Lights visible only along the runways are being installed at several airports to control taxiing movements and take-offs. For controlling landings, 90-foot arrows composed of red and green marker lights are being installed at the ends of the runways. A green arrow indicates that landings may be made from the end of the runway so marked. A red arrow shows the landing end of a runway that must not be used at the time.

The growing overseas air traffic is necessitating equipment for use as seaplane-channel marker lights. Some very interesting experiments have been conducted with battery-operated coiled-tube fluorescent lamps mounted on resilient rubber floats to meet this need. The Civil Aeronautics Authority expects to establish specifications for seaplane-channel lights when these experiments have yielded sufficient data.

Electrical Advertising

The most novel feature in the incandescent-lamp type of sign is noted in the further development of the "motion picture" type of display. In such a sign moving images from a motion-picture projector are thrown upon a screen made up of photoelectric cells which, in turn, light up a huge bank of incandescent lamps in accord with the light and shade of the projected image.

In the gaseous-conductor type of display the familiar orange-red of the neon and the blue of the mercury have become so common that in many cases the colors lose their appeal and attracting power, and the variety of effects possible through fluorescent lamps are indeed welcome to this industry. It is reported that a very high proportion of all new work incorporates fluorescent tubing.

In the smaller type of displays many very unique features have been incorporated. One popular and inexpensive form uses letters or emblems made up of glass tubing in which volatile liquid is placed. There is a reservoir or small bulb at the bottom. The heat from concealed lamps causes a continuous moving bubble effect and the fluid itself is luminous from the light emitted by the lamps.

Another type of sign has a message formed by a thin wire which serves as a negative electrode for an electric discharge in a helium-filled tube. Still another takes advantage of the phenomena of high-frequency discharge. Changeable letters are filled with neon or other gases. An oscillator is placed in the base of the sign or nearby. There is no electrical connection, yet the letters glow with a fair luminosity. Standard colored low-voltage fluorescent lamps have been effectively applied to a number of signs based on the edge-glow and transparent-mirror principles.

Lighting Standards

The Illuminating Engineering Society as the guiding body has continuously conducted researches into various fields of practice. From the industrial angle the special requirements of different processes have been studied and these findings have been reported from time to time. The philosophy behind this procedure has been discussed in Electrical Engineering[14] and in IES *Transactions*.[15]

Codes applying to airport,[16] highway,[17] and schoolroom[18] lighting have also been published.

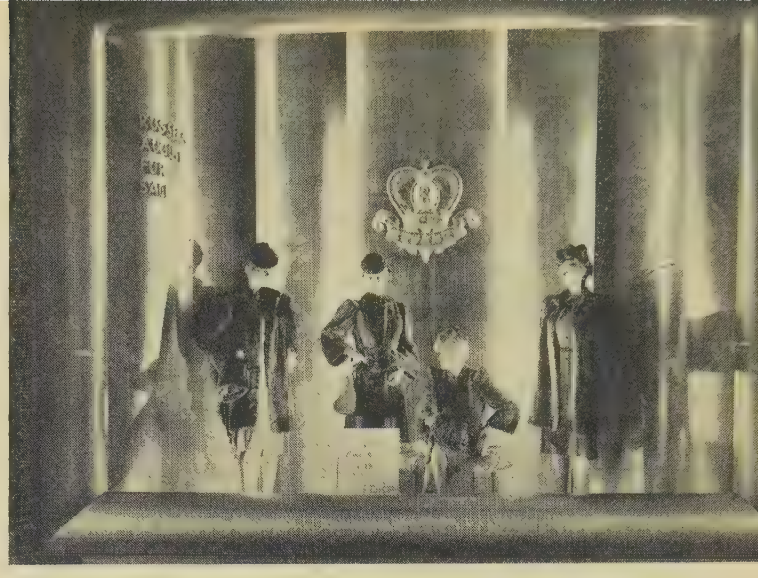
Polarized Light

Polarized light is as old as time itself, but has never been a very useful tool for the lighting engineer because of the expense and complication in producing it. Its properties are well known to all who have studied physics, and obviously if it could be made available simply, many valuable and practical effects would result. For several years investigators have been trying to produce a synthetic medium which when placed before a light source would act as a polarizer. Such a material (known as Polaroid) is now available in sheets of considerable size.[19] To the unaided eye it appears as a sheet of thin gray or neutral-colored gelatin filter. Structurally, however, in this sheet are innumerable small microscopic slivers, practically all pointed in one direction. When two sheets of Polaroid are placed with these needle-like particles parallel, maximum light transmission results. When placed at right angles to this position, practically all the light is cut off.

Polaroid mounted between pieces of protecting glass has found rather wide use in sun spectacles. Placed before the openings of reflectors directing light on paintings it provides a means of reducing specular surface reflections to a minimum. Desk and reading lamps have been equipped to furnish polarized light. Beautiful decorative effects for the theater and advertising purposes result from the action of polarized light on cellophane and similar materials.

One of the most interesting fields for the application of the polarizing principles is in connection with automobile headlights. Here the proposed scheme polarizes the light from the headlamp in a 45-degree angle and places on the windshield an analyzer at the same angle. Thus there is minimum reduction in visibility of the beam from one's own headlights, yet the light from oncoming headlights being at right angles to the analyzer, is scarcely visible.

To date, however, many practical difficulties have prevented the adoption of such a scheme. For the idea to be effective, it would be necessary to have all cars similarly equipped. Under ideal conditions the efficiency of such a lighting system would be in the order of 25 per cent. Under practical conditions such as exist at the present time, however, this figure is considerably less. Hence for the same brightness of objects to be seen, generator capacities would need to be increased more than fourfold. The proposed system would tend to work additional hardships on the pedestrian and at the same time reduce



Colored background lighting by means of fluorescent lamps in the show window of a Fifth Avenue shop in New York, N. Y.; merchandise is lighted by reflector spot lamps

the brightness of the street illuminated by standard overhead sources to at least one-half.

Lighting Control

The thermionic-tube control combined with saturable reactors has been improved in certain details. In the first stages of development it was necessary to have a reactor with a rating approximately equal to that of the load to be delivered. Thus a reactor rated for 5,000 watts would not function with a load of one kilowatt. Now, however, reactors are available that will operate over the following load ratios: 1–2, 2–1, 4–1, 10–1, and 30–1.

When this system was first introduced, it was necessary to connect reactors in cascade, which meant that different sections of an installation being thrown on the line would come up to full brightness at slightly different intervals. Now reactors are operated in parallel eliminating this distracting feature.

There has been announced a four-circuit thyratron reactor mounted on a single chassis for mobile light control.

Photoelectric relays for lighting control have been improved and a new circuit used for the control of street and

Fluorescent lighting in a New York, N. Y., fur shop

floodlighting. Less equipment and smaller space is required. The new relay is much more sensitive and turn-off and turn-on values can be very close together. Automatic control for the schoolroom has long been desired, for the teacher with her mind on a multitude of other things very often neglects the matter. Such a scheme is not practical, however, unless a decidedly inexpensive and simple photoelectric device is available. This is now the case.

Meeting of the International Commission on Illumination

The International Commission on Illumination, familiarly known to English-speaking people as the "ICI", to the French as "CIE", and to Germans as "IBK", convened for its tenth session in Scheveningen, Holland, June 11 to 20, 1939.

It drew an attendance of over 500 including 11 delegates and 9 guests from the United States. In addition to the 14 member countries, several others were represented, two other applications for membership being received.

From the American viewpoint, the session was probably one of the most interesting and productive of the ten sessions that has been held. Without neglecting standards and other basic questions, an increased amount of attention was devoted to the interchange of information and opinion on the late developments and trends in the art and science of illumination, ranging from laboratory investigation and field surveys to views as to future trends on lighting practice.

Reports covering 32 main topics were presented by 24 technical committees; the two United States secretariats submitted four of these reports. In addition to these, 34 papers by individual authors were presented for discussion, none of these being by American authors.

Among the fundamental questions taken up were the "new" candle, and the several topics arising out of the application of luminosity factors to photometry and colorimetry. Nearly all practical photometry throughout the world is now carried on by physical methods, even to the checking of visual standards.

Some additions were made to the English-French-German lighting vocabulary, which had been printed since the 1935 sessions. Arrangements were also made for the inclusion of equivalent terms in other languages in addition to Spanish and Italian.

Much information on the new electric discharge lamps was brought out as well as data on the recent developments of other illuminants. In the fields of illumination, street lighting, automobile lighting, aviation lighting, railway lighting, and school lighting, the United States Committee contributed information on a relatively extensive scale.

At the conclusion of the technical sessions, a program was adopted for the ensuing three years, 25 secretariats being allotted to the several national committees. In addition a plan was adopted for the presentation of individual papers at the 1942 sessions.

Officers were elected including Doctor Halbertsma of Holland as president and E. C. Crittenden (A'19, M'22) of the United States as one of the three vice-presidents. The invitation of the French committee to hold the 1942 Session in Paris was accepted, while an invitation from the Polish committee to hold an intermediate meeting of the aviation lighting committees in Warsaw was deferred for later action.

Now that Warsaw is in ruins and the great countries of Europe are too busy with destruction to give attention to co-operation on constructive effort, these plans presumably are canceled.

General

It is believed that the above data are a truly remarkable record of progress in an art in a very short space of time. It is impossible to give due credit to the many workers in the field of lighting. They are legion, and here and there without fanfare or publicity, without public recognition, these individuals advance the art here a little, there a little. The cumulative effects are of vast importance to the public. This work is often not sufficiently spectacular to receive much publicity, but it is none the less important and of a nature that is appropriate for engineers to recognize.

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An Armchair Spectator Conveyer-Guide

JAMES DUNLOP   W. T. WHITE

Visitors to the General Motors "Futurama" at the 1939 New York World's Fair, an extensive scale model purporting to depict the America of tomorrow, were transported through the exhibit on a specially designed and constructed conveyer consisting of a continuous train of moving chairs almost one-third of a mile long. Loudspeakers provided each individual spectator with a running account of the changing scene throughout the tour.

AS SUCCESSIVE world's fairs have grown in size and diversity, the exhibitors have built larger and more elaborate buildings, and the exhibits themselves have become more diversified and of more general interest. This trend is exemplified no better than at the 1939 New York and San Francisco fairs. What has proved to be one of the real hits of the New York fair is the "Futurama" exhibit in the General Motors Building (5,000,000 visitors during the 1939 season); it is said to be the largest scale model ever built, covering nearly 35,750 square feet and extending for almost one-third of a mile. It contains about 500,000 buildings, over 1,000,000 trees, and 50,000 scale model automobiles, 10,000 of which actually operate. Visitors are taken on a tour over a miniature cross section of America. They see mountains, valleys, and plains, cities, towns, and farming communities, rivers, lakes, and dams, and through it all run the proposed superhighways of the future with their traffic control towers, multidecked bridges and separate lanes for speeds up to 100 miles an hour. This exhibit cannot adequately be described; it must be seen. But the 140-ton 600-seat spectator conveyer, which has a capacity of about 30,000 passengers per exhibit day, and the sound system, "20 tons of voice," which replaces 150 private guides—one for every four passengers on the conveyer—are unique.

From figure 1, it may be seen that the conveyer is set up on an endless, circuitous track which has many radii varying from 9 to 98 feet with changing elevation reaching a maximum of 23 feet above the lowest point. There are

JAMES DUNLOP and W. T. WHITE are, respectively, division engineer and commercial engineer, Westinghouse Electric Elevator Company, Jersey City, N. J. Material describing the sound system, including figures 7, 8, and 9, was furnished by the information department of Western Electric Company, New York, N. Y.

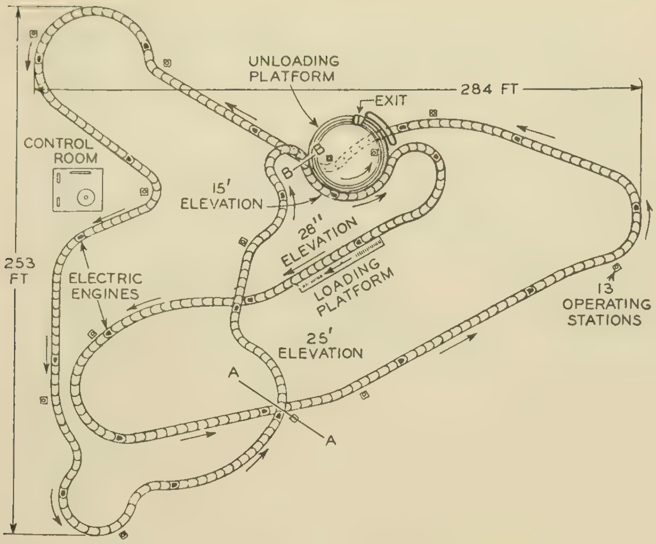


Figure 1. Plan diagram of conveyer in General Motors "Futurama" exhibit at the New York World's Fair

Length of conveyer—1,568 feet	Round-trip time—15 minutes
Electric "engines"—23	Passengers per hour—2,400
Passenger capacity—600	Crest of undulation—23 feet

Area covered—44,000 square feet

spiral sections of the track having an incline of one on six, with a radius of 18 feet.

The total length of the track is 1,568 feet. It consists of a channel-shaped center track which definitely fixes the path of the conveyer. The outer tracks are angle shapes having the flat surface horizontal to receive the wheels of the train.

Referring to figures 2 and 4, these three tracks are shown to be mounted on a structural trestle which extends throughout the entire path of the conveyer.

The conveyer consists of 322 platforms of sufficient width to carry a double chair and still leave ample passageway in front of the chairs as shown in figures 3 and 4. The surface of the platforms must be flush with each other and the space between them as they pass over the undulations must not vary enough to create a hazard.

The length of the platform is about five feet, one end being made concave and the other convex. At the concave end immediately beneath the surface is provided a channel track upon which rest small Micarta rollers mounted on pins, set radially to the center pin in its mating platform. The center pin has a soft-tired roller mounted on it which runs within the channel-shaped center track.

From figure 5, it may be noted that the platform is mounted on caster-type rollers placed on the bisective line of the chord between the center pins of adjoining platforms. This arrangement completely eliminates any tendency of scuffing or skidding of the rollers on the track. There is considerable warpage in the platform as it passes through the spiral track, but by having it supported on the tracks by only two wheels at one end and at the other end on wheels enclosed in the channel track of its mating platform, the surfaces of the platforms are kept flush with each other.

The chairs are mounted on three-point supports of deep sponge rubber so as not to limit the required warpage of the platform.

The platforms are connected by wishbone-shaped couplings designed to have rigidity in the horizontal direction only but to have flexibility in the vertical so as to accommodate themselves to the positions of the platforms at all times. The bearing at the "elbow" of the "wishbone" is mounted on the center pin of the platform, and the adjusting screws at the ends of the arms of the wishbone are fastened to a cross member of the mating platform.

This endless train of platforms must be made exactly

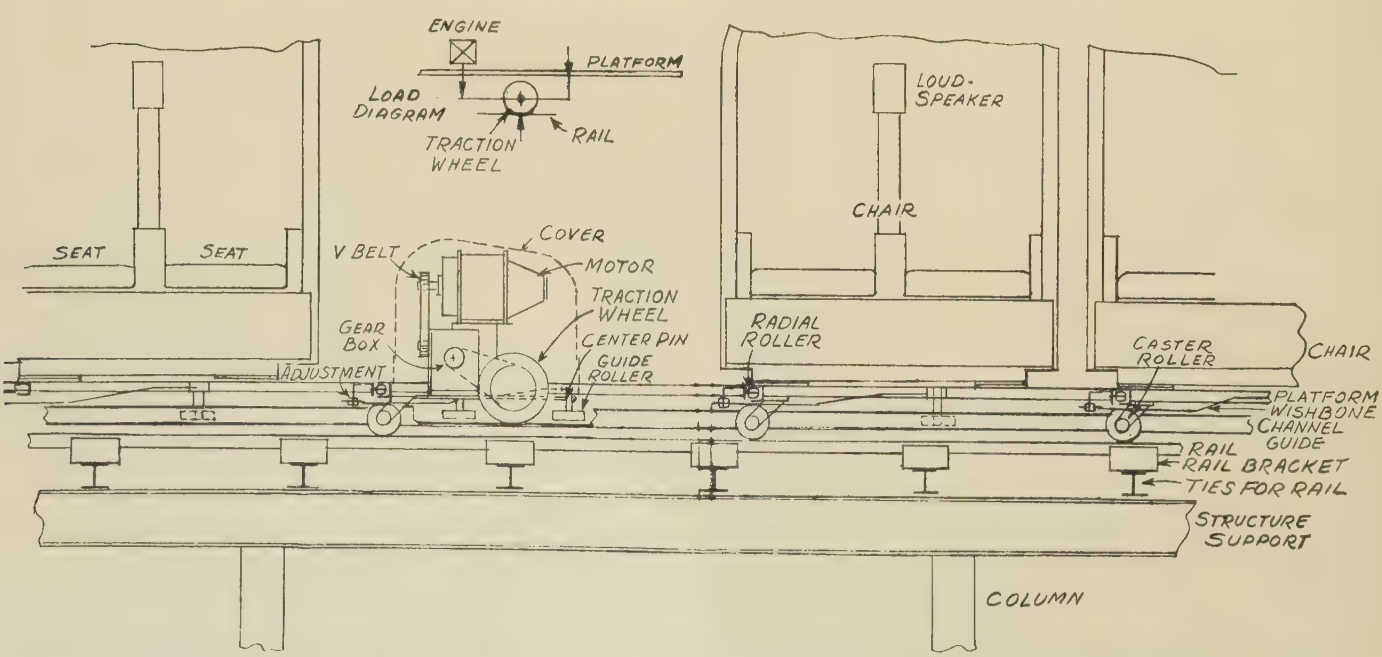


Figure 2. Sketch showing design and loading diagram of conveyer "engine," and general arrangement of running gear

the same length as the tracks, which is accomplished through the adjusting screws at the ends of the wishbones as shown in figure 5. One turn of the nut on the screw between each car and the next would represent a change in the total length of the train of 30 inches. The problem of change of total length of the track measured in platform pitch due to the cordal action of the platform pitch passing round the many radii was solved by the arms of the wishbone being made slightly corrugated; this permits of slight lengthening or shortening of the train.

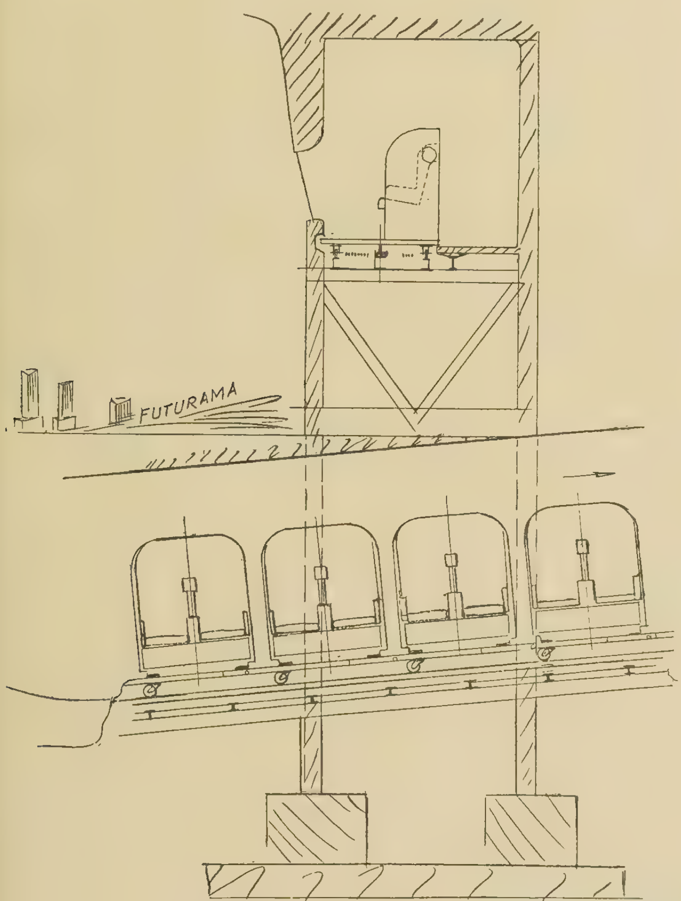


Figure 3. Sketch showing section at A-A of figure 1 where chairs at two different elevations are at right angles to each other

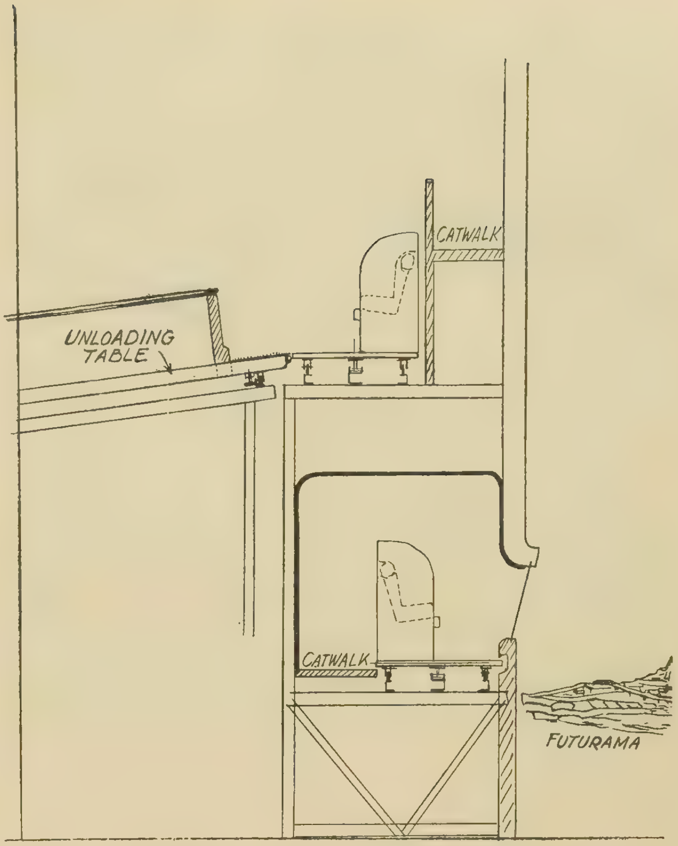


Figure 4. Sketch of conveyer at B-B of figure 1 showing chairs at different elevations facing and moving in opposite directions

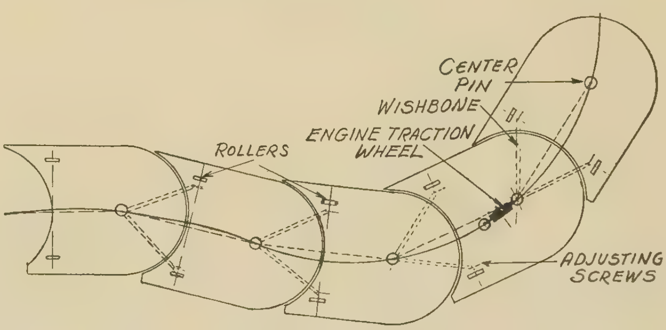


Figure 5. Sketch of conveyer platforms showing method of coupling the cars together

To drive the conveyer, 23 "engines" are furnished, one on every 14th platform. Each engine platform is provided with an opening in its center to permit the traction wheel of the engine reaching and resting on the bottom of the channel-shaped center track.

The engine consists of a two-horsepower compound-wound electric motor driving a rubber-tired traction wheel through a V belt, worm-gear reducer, and roller chain as shown in figure 2. This equipment is mounted on a sole plate which has one end attached to the center pin of the platform by a universal ball bearing. The other end has a roller fastened to it which lies in the horizontal position within the center guide track. The position of the rubber-tired traction driving wheel is exactly central between the roller on the center pin and the roller on the other end of the sole plate. Thus the traction roller is always in the theoretically correct position with respect to its travel throughout the entire path of the center track.

The load diagram at the top of figure 2 shows how the reaction of the weight of the engine and of the platforms is taken on the rubber-tired wheel, thus obtaining the necessary tractive force. Power is fed to the 23 engines through a trolley system underneath the platforms, and the connection is made to each motor through a carbon-brush system which slides along the surface of the trolley wires. These trolley wires are made of an H section, having a smooth, flat surface on top. The control is of the variable-voltage type, power being supplied by a motor generator set.

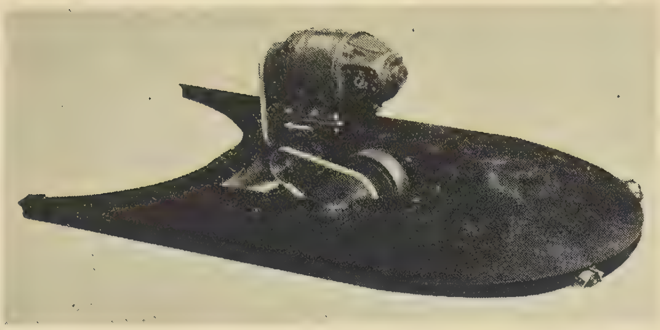


Figure 6. Top of "engine" platform with cover removed

It should be noted that the available tractive force for one engine is to be multiplied by 23 for the entire train and, since the conveyer is endless, power is applied approximately every 68 feet of its length. This multiple-unit drive gives the "lift" and the "push" that are of so great advantage in overcoming the difficulties which otherwise would be set up by the varying train resistance due to grades and track curvature and makes it possible to operate the loaded conveyer at a cost of less than 25 cents per hour.

Equally interesting is the fact that, as shown in figures 3 and 4, certain cars in the same endless train are moving, one directly over the other, in exactly opposite directions, while others travel at right angles to each other at the same time.

Sound System

Concealed in the center arm of each double chair is a loud-speaker through which the two occupants of that chair listen to a descriptive talk about the portion of the exhibit before them. The description of the exhibit is recorded on over 600 feet of sound film wound in 24 bands on a huge steel drum which rotates in synchronism with the spectator conveyer. The sound is picked up from this film by 150 photoelectric tubes, the tubes being connected through amplifiers to a system of trolley wires around the track and from these through contact shoes to the loud-speakers in the chairs. By this means each passenger is assured a description of the scene before him, which may be different from that seen by the passengers a few feet in front of or behind him. The acoustical design of the chairs prevents interference between loud-speakers in adjacent cars.

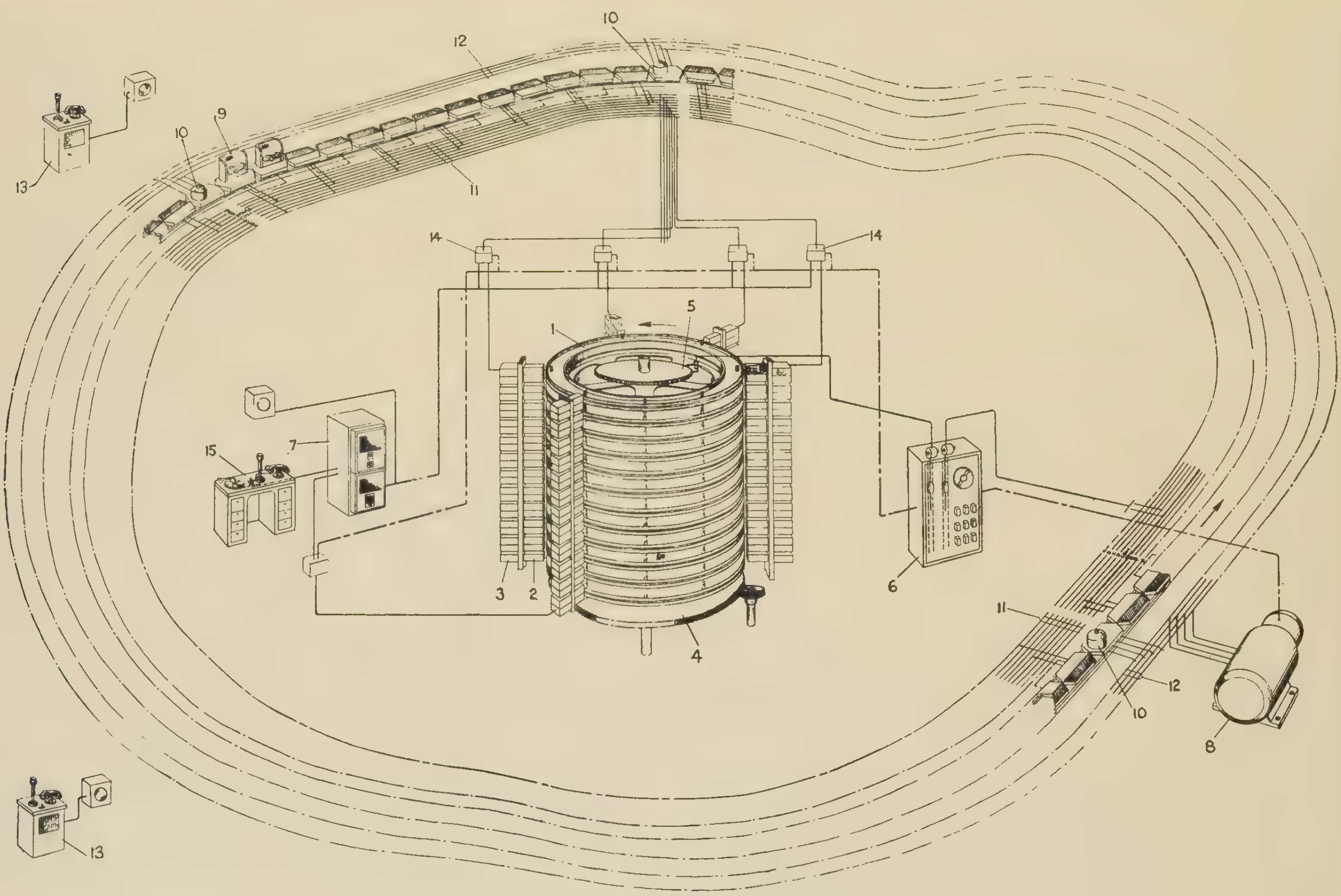


Figure 7. Diagram of conveyer sound system

1—Film-carrying drum
2—Scanning units
3—Amplifiers
4—Drive gear
5—Synchronizing cam and contactor
6—Synchronizer
7—Emergency amplifier equipment
8—Conveyer power generator
9—Spectator sound chairs
10—Motor cars
11—Sound trolley wires
12—Power trolley wires
13—Safety-officers' equipment
14—Emergency announcing relays
15—Control desk

The sound-film drum constitutes an amazing piece of precision construction for an apparatus so large. This drum is 8 feet in diameter and stands 12 feet high from a circular base housing the driving mechanism; it weighs more than seven tons. Since any variation from true in the movement of the film riding on the drum would result in sound distortion, the drum was constructed to deviate from a perfect circle by not more than 0.0008 inch at any point of its 27-foot circumference.

The drum and drive mechanism weigh about 20 tons and are supported by a mat of rubber resting on a heavy concrete foundation. This construction prevents any vibration from interfering with the photoelectric sound pickup system. To minimize expansions and contractions from temperature variations, the entire sound reproducing system is housed in a special air-conditioned room where the temperature never varies more than two degrees and where humidity is held within five per cent of its specified value.

The trolley system is also unique. It was found that a satisfactory relationship between sound and scene could be maintained for each spectator if a separate voice circuit was supplied to each pair of cars. This would appear to mean 150 different circuits and trolley wires, but space between the car rails would admit only a few. Trolley-circuit sectionalization solved this problem; seven trolley wires, instead of 150, were laid down and these were broken, with insulating joints, into 68-foot sections throughout the whole tour.

The story for the first 68-foot section of the tour is recorded on one of the 24 loops of film. This record takes 40 seconds to reproduce, which is the time required for one complete revolution of the film drum and for a given car to travel 68 feet. One of the photoelectric pickups scanning this loop is associated with one of the seven trolley circuits extending along the first 68-foot section of the Futurama. The loud-speakers in the first pair of cars in each 68-foot section connect with this particular rail. Sliding contact is made through a silver-impregnated shoe beneath the car.

When the rotation of the drum is properly synchronized with the speed of the cars, this shoe first touches its trolley wire just as the story is commencing. As the story finishes, for that 68-foot section, the contactor shoe is carried to the next section of the same wire where the story is continued from a second loop of film. Similarly, all the remaining sections of trolley wire, fed from other film loops, tell consecutive parts of the story until the cars have completed one round trip.

In addition to this first scanner, six others are spaced at equal intervals around the loop of film, as previously mentioned. Each of these scanners connects with some one of the six remaining trolley circuits in each section of track. All 24 loops are so scanned by seven pickups. Obviously, the beginning of the story recorded on each loop arrives at each scanner progressively. By connecting each pair of cars in any one section with the proper trolley wire, sound matching the scene before that pair of cars will be projected to the spectator.

An eighth trolley wire, paralleling these seven, provides a common return path for all speech circuits. In addition, a short section of wire, slotted into approximately two-foot lengths, extends along the loading zone for synchronizing purposes.

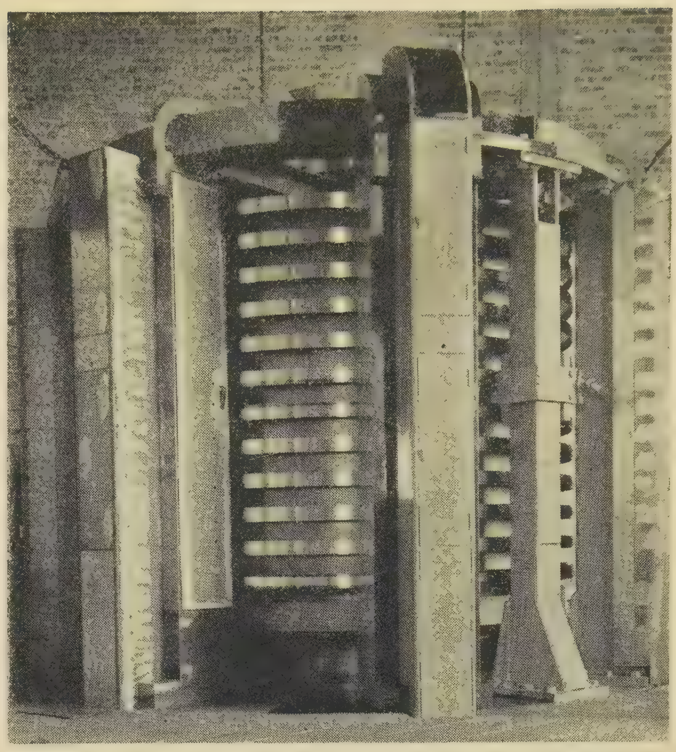


Figure 8. A view of the sound drum. A loop of sound film is wound around the periphery of the drum at the top and bottom of each of the 12 rings

Synchronizing Conveyer and Sound System

Since the sound for the Futurama is produced by a film-carrying drum rotated by synchronous motor at constant speed past photoelectric-cell pickups, this machine sets a pace that the conveyer must duplicate if the correct sound is to be reproduced on the proper loud-speaker. Synchronization is accomplished as follows: Two similar auxiliary devices are provided, one of which is made to move in exact relation to the movements of the sound machine while the other moves in the same relation to the movement of the conveyer. This is accomplished by the use of two motor-driven carriages one of which moves in response to impulses set up by the rotation of the sound drum, while the other in a similar way responds to the movement of the conveyer. Thirty-eight impulses drive the sound carriage the length of its travel, which corresponds to one revolution of the sound drum. Thirty-eight impulses also drive the conveyer carriage the length of its motion, which covers about 68 feet of conveyer travel. In this distance a given point on the conveyer travels past as much of the Futurama as can be described in one rotation of the sound drum.

The two motor-driven carriages are arranged with brushes contacting segments and cross connections such that when they move together no action occurs. If the conveyer carriage lags behind the sound carriage a relay picks up to increase the conveyer speed by raising the main

generator voltage, and the opposite occurs when the conveyer carriage gets ahead of the sound carriage.

To start the conveyer and have it pull into step automatically the following sequence occurs: When the starting circuit is completed the conveyer starts at slow speed, about five feet per minute. The sound drum is rotating at a constant speed of 104 feet per minute. The two carriages are moving at proportional speeds, and after a few seconds the sound carriage approaches the position of the conveyer carriage. At a predetermined point before the sound carriage overtakes the conveyer carriage, corresponding to about eight feet of conveyer travel, the conveyer is accelerated to its full speed (104 feet per minute). While it is accelerating the sound carriage is overtaking the conveyer carriage and as the conveyer reaches full speed the two carriages are in the synchronized position. The automatic synchronizing circuits previously mentioned maintain this relation from this time on.

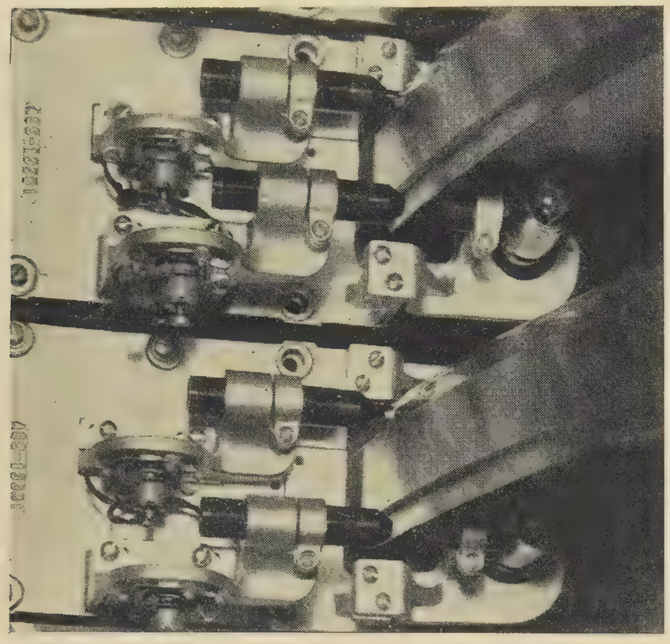


Figure 9. Close-up of photoelectric pickups which scan the sound films wound around the drum. Axial deviation of the film does not exceed 0.0004 inch

The conveyer itself runs through a soundproofed and air-conditioned tunnel with glass windows on the side faced by the passengers who thus get the effect of viewing the entire Futurama from a low-flying airplane. Directly behind the conveyer in the tunnel is a continuous catwalk with convenient exits to the building. In case of an emergency shutdown any passenger could easily walk not more than 20 feet over the interlocking platforms to one of the openings leading to the catwalk.

The conveyer system is controlled from a main control station located in the sound-system control room where a supervisor is continually on duty. Located around the track and connected to the main station are 13 other control stations each with an attendant on duty at all times. To start the conveyer each attendant must press his starting button which will light a lamp in the main control station. When all stations have given the starting signal the supervisor can then start the conveyer from the master control board. The stop button in any station, when pressed, will immediately stop the entire conveyer and is held in magnetically until released by the attendant.

Loading and Unloading Platforms

The loading platform is practically the equivalent of a horizontal electric stairway 40 feet long with horizontal treads which are level with the platform of the conveyer and travel at the same speed. It has comb plates at each end and on the outside has balustrading, together with a moving handrail. Passengers feed onto this loading platform just as they would enter an electric stairway and then step directly onto the platform of the conveyer and take their seats. If, for any reason, anyone should decide not to board the conveyer, he may simply proceed to the discharge end of the loading platform and step off.

The circular unloading platform is a metal ring approximately 32 feet in diameter inclined at an angle of five degrees. Its surface is covered with continuous aluminum cleats and its edge is made with a series of flats which accurately engage the edges of the conveyer platforms; the latter curve around part of the unloading platform so that several spectator platforms may be flush with the unloading platform at one time. A balustrade is provided continuously on the inside edge of the unloading platform and of course revolves with it. At the discharge where the conveyer swings away from the unloading platform, a similar but stationary balustrade with a moving handrail is provided parallel with the outer edge of the unloading platform together with a comb plate at the end. A passenger having completed the trip rises from his seat, steps onto the unloading platform which is at the same level as the conveyer platforms and moving in synchronism with them. Almost immediately he reaches the moving handrail, takes hold of it, and is conveyed down to the comb plate where he alights.

Credit for this remarkable exhibit is due in great measure to the vision and courage of William S. Knudsen, president of General Motors Corporation, the co-operation of D. C. McGuire, general manager of the General Motors Argonaut Realty Division, and the ability of Norman Bel Geddes, the designer, Albert Kahn, Inc., the architect, George Wittbold, the exhibit engineer on diorama, and the Turner Construction Company, the general contractors. Electrical Research Products, Inc., designed and (with the exception of the sound drum itself which was built by the Westinghouse Company) built the extremely intricate sound system. Westinghouse Electric Elevator Company furnished the spectator conveyer with its loading and unloading platforms, and the special devices that synchronize the conveyer and the sound system. James Dunlop, division engineer for the Westinghouse Electric Elevator Company, was responsible for the successful and unusual design of the complete spectator conveyer, and W. F. Eames handled the electrical equipment and designed the synchronizing devices.

The Human Science

C. T. WELLER
MEMBER AIEE

LIKE many other words in our language, "science" has more than one meaning. It may be defined as systematized knowledge in general and also as any branch thereof. When we think of the various branches of science, we are apt to stress those like astronomy or metallurgy that are inanimate, inorganic, impersonal. Occasionally, therefore, it is well to remember that there is another science, a very human one—anthropology—the science of man himself. As might be expected from the wide range of human activities, anthropology is a very broad subject and can be here outlined only in part. In so doing, that sword of Damocles, the possibility of error, which hangs over the head of every scientist, is acknowledged at the start.

Those who can remember back half a century may recall some of the theological controversies then current. I am told that one of them involved the question as to whether or not the Creator had limited himself in any way. One country preacher had very strong convictions on this question. He contended that not even the Almighty could create a two-year-old heifer in a day. He conceded that a heifer could be created that would resemble the two-year-old heifer in all respects except one; she would *not* be two years old. This proposition might be restated as gradual versus instantaneous growth. From a scientific standpoint, we seem to have no evidence of instantaneous growth, but we do have considerable evidence of gradual growth which it might be of interest to review.

Short-Term Growth

Everyone is familiar with the growth of a child. Doctor Steinmetz in his book on "Engineering Mathematics" pointed out that each child during its first years of life repeats the development of the race throughout the ages. While physical growth slows down toward maturity, mental growth continues. A few moments' introspection will doubtless evoke indulgent smiles as we recall how naïve we were only a few years ago. What has caused the changes noted? Equal increments of time and unequal increments of experience have somehow produced diverse results, and the chances are that today we are quite different mentally from what we were say a decade ago. Perhaps we have acquired a broader outlook, a firmer grasp on fundamentals, or a more effective discipline. Although somewhat uneven, this growth, like that of a child, by and large has been gradual. Yet what seems gradual when compared with the complete cycle of the individual becomes swift indeed when compared with the time elapsed since the beginning of life on the earth. Thus the individual cycle can be considered as short-term growth.

Long-Term Growth

What of man's long-term growth? For an excellent example of long-term growth, we turn to our good friend the horse. The first known ancestor of the horse was an animal about the size of an ordinary cat, having several toes on each foot. This ancient animal was so different from a modern horse that the relationship at first was not suspected. It required the discovery of intervening types of increasing size and decreasing number of toes finally to establish the relationship. In passing, it is interesting to note that our large, one-toed mammalian friend is fast disappearing from city streets, even in our time.

At present, no comparable evidence of man's long-term growth is available. Our earliest ancestors could not have been very numerous and they were apparently too shrewd to become mired in Californian or Caucasian asphalt pits or equivalent preservatives. Only in a few favored caves have their remains been found, supplemented by accidental discoveries during excavations for other purposes and from exposures caused by erosion.

In America, the human frontier has been pushed back a few thousand years by the discovery of artifacts or primitive equipment of the Folsom man, whose arrowheads incorporated modern ideas of streamlining. This race was prehistoric, whether measured by the written word or by tree rings, and was doubtless beyond the reach of legend. It may seem strange at first even to consider legends in searching for the remains of our ancestors, but we need all the help we can get.

The assistance that legends are capable of giving might be illustrated by the Chinese dragons. These dragons were long thought to be the product of someone's heated or even fermented imagination. However, the discovery of the great living reptiles on the Island of Komodo in the Dutch East Indies showed that there was a physical basis for the dragon legends. Similarly, it is believed that the European legends of ogres were based upon the Neanderthal man; survivors of this race apparently were contemporaneous with our more immediate ancestors. That such a survival is possible is shown by the primitive tribes now living in Australia and elsewhere. Many specimens of the Neanderthal man have been found in Europe, from which his rather repulsive appearance has been inferred.

More ancient than the Neanderthal man is the Peking man whose remains have been found in a cave at Choukoutien in China. The relative age of these races is judged partly by the tools they made and used and by the remains of contemporary animals found nearby.

Most ancient of all apparently is the Trinil man, a few specimens of which have been found near Trinil on the Island of Java. This is not far from the home of the great dragon lizards. So low in the relative scale is the Trinil man that some doubt has been expressed as to his being really human.

A talk broadcast June 22, 1939, during the General Electric Science Forum program, from radiostation WGY, Schenectady, N. Y.

C. T. WELLER is an electrical engineer in the general engineering laboratory, General Electric Company, Schenectady, N. Y.

Man's long-term growth may be summarized by stating that he has evidently lived on the present plateau for a very long time and has previously lived for much longer periods on other and definitely lower plateaus. What has made it possible for him to scale these successively higher plateaus? Apparently not any increase in physical dexterity or even the acquisition of more efficient tools, but rather the gradual development of the higher faculties of the mind.

Future Growth

What of the future? There seems to be no reason to expect that man's long-term growth will not continue and that plateaus higher than the present one will not be reached. Whether such advances will be due to further development of the mind, to a better understanding and control of the primitive subconscious, to an expansion of the senses, to a combination of these factors, or even to other factors not now suspected, cannot be forecast.

Pertinent to these prospects is a current investigation. It has been recognized for centuries that a few individuals in each generation have possessed a sort of sixth sense known as clairvoyance. The present efforts might be regarded as a quantitative analysis of clairvoyance, which is now called extrasensory perception. The results obtained show that the average score of those tested as to their ability to select or identify unseen items far exceeds the value expected from the theory of probability. The soundness of the methods used and the accuracy of the theory itself have therefore been challenged. This controversy, which is only one of many involving the various aspects of anthropology, should not be permitted to obscure the significance of the subject. If but ten per cent of the population were found to be gifted with extrasensory perception, it would be pregnant with possibilities.

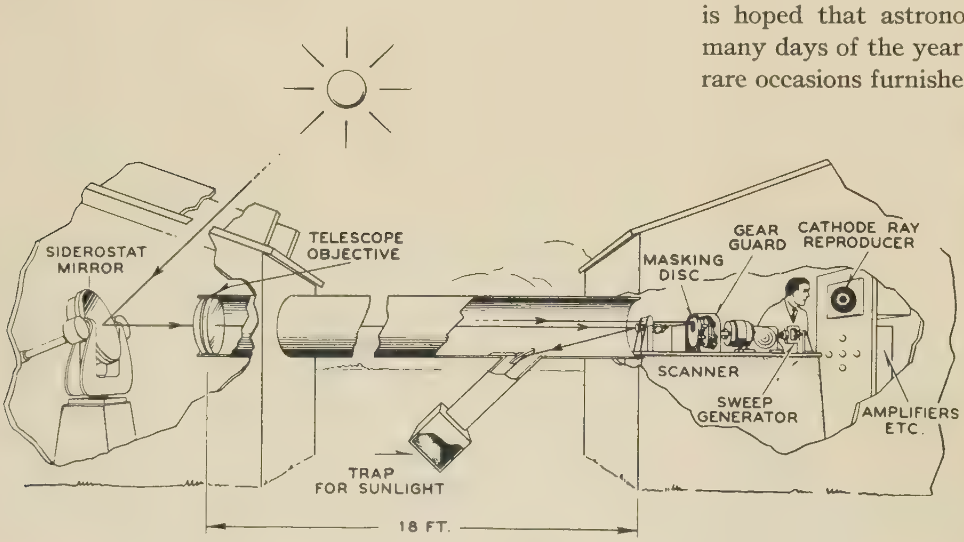
The controversies referred to raise the question as to what positions should be taken. In this respect, attitude is more important than position and I believe each of us can make a very definite contribution by endeavoring to maintain an open mind—open toward the past, open toward the present, open toward the future.

Device for Viewing the Solar Corona

ASTRONOMERS have long wished for some system that would enable them to study the solar corona—that flaming halo around the sun—by disregarding the powerful beam of the sun and the steady glare of the sky, and concentrating on the corona itself. Radio research men also have been interested, because the major disturbances of long-distance radio transmission have their origin in the sun and studies to date have indicated that a day-to-day knowledge of the activity of the corona might prove useful in predicting transmission conditions. Knowing of this need, it occurred to Doctor A. M. Skellett of Bell Telephone laboratories, New York, N. Y., that a television circuit had the necessary discrimination between steady light and variations. Apparatus embodying his idea was built in the laboratories and has been given a practical trial. A paper describing the device was presented at the recent Providence, R. I., meeting of the National Academy of Sciences.

Briefly, the apparatus (see diagram) is a television system that scans a ring around the sun. Light from the sun enters a horizontal telescope, and a sharp image of the sun is focussed on a mirror. Since this is of no use, it is reflected into a trap and dissipated. Around the image of the sun is that of the corona, and that is scanned by a combined lens and mirror. Light from this scanner is thrown into a photoelectric cell, where a direct current of varying magnitude is generated. Such a current may be considered as made up of two components: a steady current, due to light from the sky; and an alternating current due to the appearance of coronal features. In the amplifying circuits, the former is discarded and the latter is brought up to a level at which it will actuate the cathode-ray tube of a television receiver. The image may be viewed directly or recorded photographically (see illustration).

Although the development of an adequate instrument and the proving in of the method have been achieved, the real capabilities of the device will be realized only when it is used under the crystal-clear skies on a mountain top in conjunction with a telescope that, by pointing directly at the sun, will eliminate most of the glare introduced by the horizontal mounting. Under these ideal conditions it is hoped that astronomers may observe the corona on many days of the year instead of having to wait for those rare occasions furnished by solar eclipses.



Philadelphia A-C Network—Operating Results

PAUL W. CROSBY
ASSOCIATE AIEE

Twelve years' experience with the Philadelphia low-voltage a-c network, which differs materially from the more common types of networks, has proved it capable of meeting all the requirements of simplicity, reliability, and economy in the supply of high-grade electric service to the central business district of the city.

THIRTEEN years ago a paper was presented before the Philadelphia Section of the AIEE describing a low-voltage a-c distribution network for the central business district of Philadelphia. That paper described what was then a proposed system, a system that was entirely new and, in many respects, fundamentally different from the a-c network systems then being developed and placed in operation in other cities. Although thoroughly engineered and tested in the laboratory, there was no background of previous operating experience for this type of system. This network, which incorporated several novel features never before used in a distribution-system protective scheme and possessing a number of inherent advantages over other types of network systems, has long since passed the theoretical or experimental stage. It is therefore timely that the performance of the Philadelphia type of network be reviewed on the basis of operating experience since its establishment 12 years ago.

A low-voltage network may be defined as "any distribution system in which transformers at various locations have their secondary circuits tied together through the distribution mains." The transformers supplying such a network are usually fed from a number of separate primary feeders, which in turn may originate at one or more substations.

The a-c network is the modern method of electric distribution in the high-load-density areas of nearly all large cities today. Although modern, the network principle is not new, since even the early d-c distribution systems consisted of a grid of mains joined together at every street intersection. A very high degree of service reliability was obtained with the d-c network.

It was long recognized that the ideal method of a-c distribution would be a network similar to the d-c system. With such an interconnected system, all customers could be supplied from the same set of mains; advantage could be taken of diversity in the loads to install less primary-feeder and transformer capacity than would be required on a radial system, where each part of the system must be able to supply the maximum demand of its connected load; and, most important, a high degree of service continuity could be obtained. Quite some time elapsed, however, before a-c systems were developed to a point where they could compare favorably with the record of service reliability obtained with the d-c system.

A-C Network System—General

The problem with any interconnected system was that of providing means whereby faulty parts of the system could be isolated effectively and reliably without impairing service. This problem has been solved in the modern a-c network.

All secondary mains in a given area are connected together to form a solid grid of conductors. This grid is fed from a number of transformers which are supplied by a group of independent primary feeders. Protective systems have been developed whereby a failure on one feeder does not cause an interruption of service, since the load is carried on the remaining feeders. The failure of a transformer does not cause an interruption of service, since the load is carried on adjacent transformer banks. It is apparent that if a fault occurs on a primary feeder, even though the feeder breaker opens in the substation it is necessary to disconnect the feeder from the network to prevent a back-feed from the network into the fault.

The conventional type of a-c network system in operation today consists of a solid grid of secondary mains supplied from a group of *radial* primary feeders. The transformer banks are connected solidly to the primary-feeder cable and feed into the network through automatic network protectors. The automatic network protector is a low-voltage circuit breaker, automatic within itself for both tripping and reclosing under predetermined conditions by means of relays.

A fault on a radial primary feeder causes the feeder circuit breaker at the substation to open from excess current, and the automatic network protector on each transformer bank connected to the faulty feeder to open on reverse power flow from the network. The entire feeder and all of its associated transformer banks are thus removed from service, the load being carried by the remaining primary feeders and adjacent transformer banks. Similarly, when a fault in one transformer develops sufficiently, the entire primary feeder and all of its transformer banks are isolated. After repairs have been made, the feeder breaker is closed and all the associated network protectors reclose automatically, providing conditions are such that power will flow into the network and phase relations are correct. Faults on the secondary mains are depended on to burn clear as in the old d-c system.

It should be noted that in the method of radial feeder supply to a network, the entire primary feeder and all transformer banks associated with it are removed from service in event of a failure on any part of the feeder.

A local Section prize paper, presented at a meeting of the AIEE Philadelphia Section, May 8, 1939.

PAUL W. CROSBY is an electrical engineer with the Philadelphia Electric Company, Philadelphia, Pa.

The foregoing is a brief description of the a-c network system in operation in nearly all large cities today.

The Philadelphia A-C Network System

In many respects, the a-c network in Philadelphia is fundamentally different from the usual type of system found in other cities. The Philadelphia system consists of a number of interlaced *loop* primary feeders, sectionalized by means of automatic oil circuit breakers, supplying a fused secondary network as shown in figure 1. Automatic network protectors are not used. The protective scheme consists of (1) a simple balanced circulating-current pilot-wire system which trips the primary-feeder sectionalizing breakers of only the faulty loop section, and (2) copper link fuses in the secondary leads of the network transformers and in the secondary mains.

The area covered by the Philadelphia a-c network is approximately 1.4 square miles, extending from the Delaware to the Schuylkill River, Market to Pine Street, and from the Delaware River to Broad Street, Market to Vine Street. The maximum demand of the network load is about 30,000 kva, and it is supplied from 26 2,300-volt primary loop feeders. The total network area is divided into three separate and distinct networks. Each of these networks is supplied from two substations, and each such pair of substations is fed from the same generating station by 13,200-volt transmission lines. Each substation has emergency 13,200-volt tie lines to another substation supplied from a different generating station.

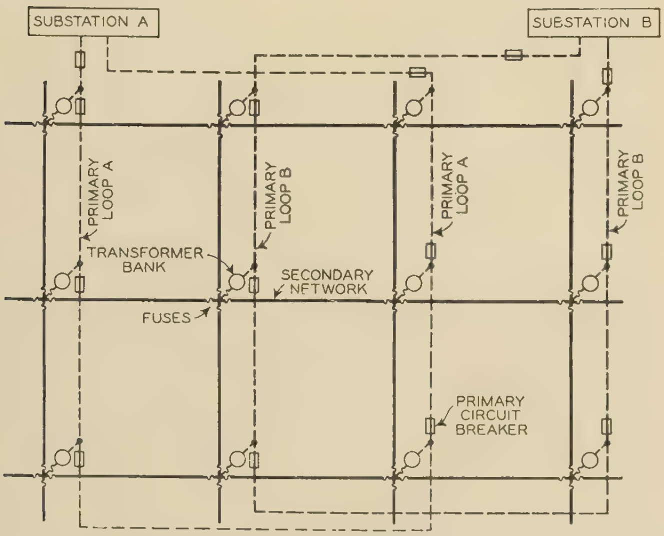


Figure 1. Secondary a-c network supplied from primary loop feeders

Both substations *A* and *B* are supplied from the same generating station, but have emergency tie lines to substations supplied from a separate generating station

The primary loop feeders supplying the network are three-wire two-phase circuits and are operated at 2,300 volts. On all but a small portion of the network, three-conductor 350,000-circular mil 13.2-kv cable has been installed to provide for possible future operation of the loop feeders at 13.2 kv. Each loop feeder extends from a substation through a number of intersections throughout the network area and returns to the same substation. Each feeder is routed through widely separated parts of the network area and interlaced with other feeders, so that so far as practicable adjacent transformer banks are supplied from separate feeders. A four-conductor cable, used for the pilot-wire protective system, parallels the primary-feeder cable.

The oil circuit breakers used for sectionalizing the primary loop feeders are of the subway type, automatic trip and manual reclosing, rated at 15,000 volts and 400 amperes, with an interrupting capacity of 100,000 kva at 15 kv. Each breaker contains six bushing type current transformers and six trip coils for the balanced pilot-wire protective system.

All of the transformers used on the network are 100 kva single-phase subway-type units. The early installations were made with standard 2,300/115–230-volt transformers with current transformers for the pilot-wire system installed in the risers of the secondary leads inside the case. All the transformers in the two Delaware network areas and about 40 per cent of the transformers in the Schuylkill network are rated at 13,800–2,300/115–230 volts. These special transformers are constructed with their high-voltage windings in six sections, connected in parallel for present operation at 2,300 volts and arranged for series connection for operation at 13,200 volts with ratio adjusters and taps for Scott connection if desired in the future.

The secondary network is a five-wire two-phase 115/230-volt system. Mains consist of three-conductor and single-conductor 350,000-circular mil 600-volt cables, generally paper-insulated and lead-covered. At the transformer intersections five-way single-polarity sectionalizing boxes are installed to junction and fuse the transformer secondary leads and street mains. Both lighting and power loads are supplied from the same set of secondary mains.

Balanced Pilot-Wire System of Protection

Each primary loop feeder is composed of a series of unit sections, each section extending between sectionalizing oil circuit breakers. The network transformer bank and sectionalizing breakers are included in the unit section. Figure 2 is a schematic diagram showing the protective system of a complete primary loop feeder. Each unit section, shown in detail in figure 3, consists of three branches around the point where the primary winding of the network transformer is connected to the feeder. Current transformers in each of the three branches of the unit section are interconnected to form the balanced circulating-current pilot-wire circuit. Trip coils in the oil circuit breakers are connected across the pilot-wire circuit at each end of the unit section.

Under normal operating conditions, the relative polarity of the current transformers is always such that circulating current flows in the pilot-wire circuits, with zero or negligible current in the trip coils. In the event of a fault anywhere in a unit section, primary cable, or transformer,

there is a change from the normal relative polarity of the associated current transformers; the normal pilot-wire balance is disturbed, so that current flows through the trip coils, tripping the oil circuit breakers at each end of the faulty unit section. The back-feed from the low-voltage network blows the fuses in the transformer secondary leads and that one unit section is completely isolated. Back-up protection, or a second line of defense in the event of failure of a sectionalizing breaker to operate, is provided by means of overload relays in the substation and the fuses in the network-transformer secondary leads.

The protective system for each unit section of feeder is responsive only to fault conditions within the section. Only one transformer bank and its associated section of primary cable are isolated in case of a primary-cable or transformer fault. All the other transformer banks remain in service, being supplied from the two parts of the loop feeder that have been separated by isolation of the faulty unit section. The occurrence of a fault thus causes a minimum of disturbance to the remainder of the system and permits the installation of a reduced amount of reserve transformer capacity, irrespective of the number of primary feeders.

One of the outstanding features of the sectionalized-loop-feeder system with pilot-wire protection on the individual unit sections is the protection provided for faults in network transformers. The absence of such protection is a recognized problem in the conventional automatic protector system, in which the overcurrent relays at the substation cannot be set to respond to certain types of transformer faults until the faults have progressed to an undesirable degree. Notwithstanding this added feature, the sectionalized-loop-feeder protective system is extremely simple, an important factor in distribution-system equipment that must be installed at widely scattered locations in a distribution area under adverse conditions frequently encountered in underground vaults.

Operating Results

The operating record of the Philadelphia network system is one of highly satisfactory performance. During the 12-year period of network operation, there have been 70 primary-cable and 14 transformer failures. In every one of these failures the faulty equipment was isolated successfully, without any evidence of unsatisfactory breaker performance, and, with one exception, without interruption to any network customer.

In one of the 70 primary-cable failures an open trip-coil circuit on the sectionalizing breaker adjacent to the fault prevented operation of the breaker. In this case three sets of transformer secondary fuses were blown and the station breaker opened on one end of the loop feeder. Here the inherent self-protecting characteristic of the system is apparent. A cable failure was followed by the non-operation of the protective device, and, in spite of this coincidence, no load was lost and no thermal failure of cable or transformers resulted—a test of the adequacy of the back-up protection.

Three of the 14 transformer failures were associated with primary-cable failures. The primary sectionalizing breakers operated, but the transformer secondary fuses either failed to blow or did not blow soon enough to prevent damage to the network transformer. These transformers were each located at network intersections where, because of the relatively high impedance to adjacent transformer banks or a limited number of adjacent banks, the fault current was not sufficient to assure prompt operation of the fuses. No network customers were interrupted on these transformer failures.

In another case of delayed and incomplete fuse blowing on a primary-cable fault, an undersized transformer neutral lead became overheated and caused a fire in a manhole at a street intersection. It was necessary temporarily to interrupt the mains for a block in each direction from that intersection so that a man could go into the manhole and cut free the faulted cable. This temporary interruption was the only case of network customers being isolated on a primary-cable fault. Smaller fuses are now being used in locations where the back-feed is limited, and prompt fuse blowing is now assured on primary-cable faults.

There have been a few cases of sectionalizing breakers opening and isolating unit sections for causes at the time unknown. In some of these cases, the same section tripped two or three weeks later and a cable fault was

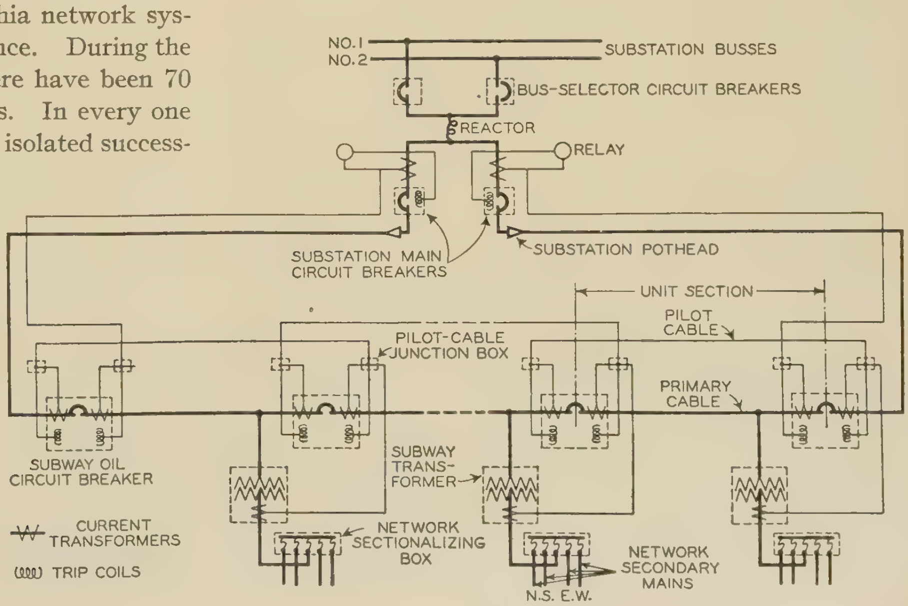


Figure 2. Single-line diagram of sectionalized loop primary feeder supplying fused secondary network, showing protective system

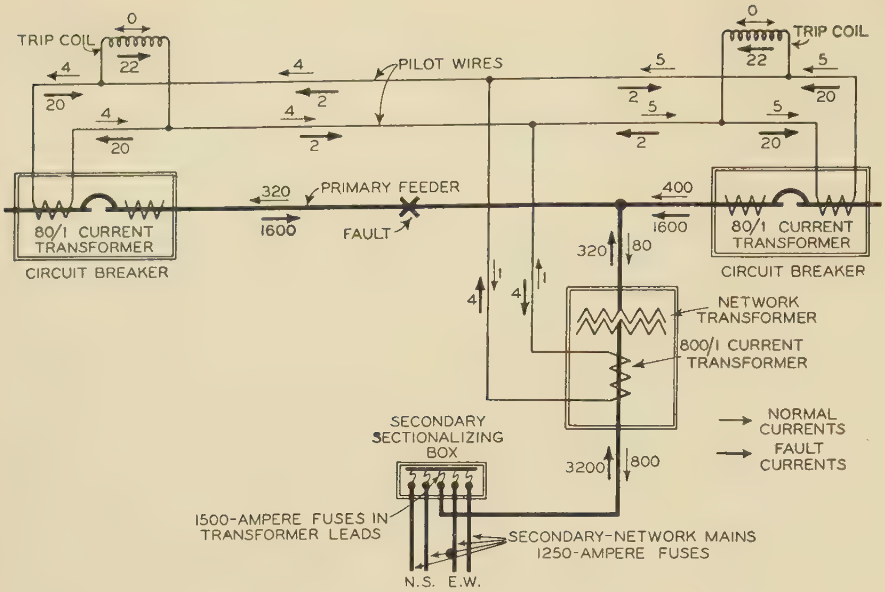


Figure 3. Schematic diagram of unit section of primary loop feeder, showing operation of protective system

Current values shown are assumed for purpose of illustration only

located on this subsequent tripping. There have been two operating errors, one of which caused an entire loop feeder to trip out, and the other caused a unit section to trip. No customer interruptions resulted from these operations.

The use of fuses in the secondary mains of the Philadelphia network is another fundamental difference from the usual type of network system, although in recent years fuses have been installed in several networks in other cities. One of the basic principles in the original design was to provide a means for isolating faulty equipment from the system just as quickly as possible, with a minimum of disturbance to the remainder of the system and a minimum of damage to the equipment at fault. The operation of secondary fuses has demonstrated that faults on secondary mains are quickly and selectively isolated, consequently with minimum damage to cables and minimum disturbance to customers on other mains.

Fuses in the secondary mains have not resulted in entire block outages at times of cable-main faults, as might be expected. Experiences to date have been that fuse blowing occurred at only one end of the main, the fault being finally cleared by burning open, with interruption of service to only a few customers in the block. Furthermore, the operating records indicate that in 84 per cent of the secondary main trouble, the fault has burned clear at the point of origin and no fuse blowing occurred.

Fusing of mains at the network intersections is desirable for other reasons, such as the protection against transformer failure and cable damage from continued overloads which would be imposed during the burning-off of tenacious faults on a solidly tied-in network. From reports on the operation of solidly tied-in networks in other cities, the clearing of secondary-main faults is a problem that is warranting serious study by some operating companies.

For the successful operation of a fused-secondary network, it is important to have a reliable degree of selectivity between the transformer and secondary-main fuses because, if the transformer fuse should blow ahead of the fuse in the main, in the event of a main fault, it would reduce the total fault current and reduce the certainty of the fuse blowing in the main. The result would probably be a sustained overload on adjacent network transformers and cable mains with possible thermal failure of transformers and other mains. Operating experience has conclusively demonstrated that the selected sizes of transformer and main fuses give the necessary degree of selectivity. During the 12 years' operating experience, no transformer fuse has ever blown on a main fault, nor has a main fuse ever blown on a primary-cable or transformer failure. The fuse sizes are selected to be responsive only to currents of fault magnitude, 1,500- and 1,250-ampere fuses being used in the transformer leads, and 1,250- and 1,000-ampere fuses in the secondary mains. The fuses are of the simple copper link type with ratings based upon full load.

In the early development of the original system design, particular attention was paid to the ratio of current transformers, the size of pilot wire, and the volt-ampere characteristics of the a-c trip coils of the sectionalizing circuit breakers. It is important that the current transformers have similar characteristics throughout their operating range and that the voltage drop across the pilot-wire circuit be limited to a value that will avoid faulty operation of sectionalizing circuit breakers on a through fault, that is, a fault in another section of the loop feeder, or on the network. The factor of safety against such faulty operation on through faults is dependent, among other things, on the length of section between sectionalizing breakers and on the magnitude of the current through the section.

There have been instances of faults involving the flow of fault current through sections from 3,000 to 5,000 feet in length, none of which resulted in faulty operation of breakers at the ends of the long sections. Though the fault impedance may have been such that the fault current was limited, this was evidently not the only factor preventing incorrect tripping, since one fault was a solid three-conductor short-circuit to ground and involved a section 3,500 feet in length.

Although designed initially for the supply of service at secondary voltage, it has been found that the sectionalized loop feeder system lends itself readily to the supply of service at primary voltage. This has been done flexibly, and with a high assurance of continuity of service, closely approaching that afforded by two services from separate radial primary feeders. The primary services have been tapped directly to the loop sections without any additional sectionalizing facilities. The high degree of assurance of service continuity arises out of the fact that, based upon

operating experience during the past several years, the probable frequency of faults on a particular loop section is about once every 25 years.

Major System Disturbances

The individual network areas have successfully coped with several interruptions of supply to one or both of the substations supplying them. With the loss of supply to one of the two substations feeding a network area, the other station continued to carry not only the network load but also, by back-feed through the network and loop feeders, a number of radial feeders from the substation in trouble. The maintaining of service by back-feed from the network, at such times, is in marked contrast to the operation of the usual type of a-c network system, in which all transformers of the interrupted substation would be disconnected from the network on reverse power flow through the network protectors.

In the cases of simultaneous loss of supply to both substations feeding a network area, service was readily restored without operations of loop primary sectionalizing circuit breakers or blowing of secondary fuses. With such disturbances, the resumption of normal supply conditions has been found to be an absolutely clean-cut and quick procedure.

In major disturbances, the network load and the radial load from network substations have been carried with either no interruption or (in the event of loss of supply to both substations) with only a short interruption because:

1. Supply from another generating station is immediately available to each network substation via tie lines.
2. All network feeders and transformers remain connected because the protective system is unaffected by direction of power flow at any single point.
3. Reserve capacity is inherent in the primary loop feeders.
4. Secondary main and transformer currents do not blow fuses.

Inspection of Network Equipment

The simplicity and ruggedness of the loop oil circuit breakers and fused sectionalizing boxes has resulted in low operating and maintenance costs. The protective equipment is called upon to function only under fault conditions in a single unit section and this is undoubtedly an important factor in reducing the operating and maintenance costs on the protective equipment.

An inspection is made every 6 to 12 months to determine the condition of transformers, oil circuit breakers, secondary sectionalizing boxes, and pilot-wire control system. Because of the simplicity of the network equipment, the tests and inspections are relatively simple and easily made. There are no relays or moving contacts in the control circuit, nor any equipment requiring highly sensitive and "hair trigger" adjustments for satisfactory operation.

Routine inspections include the checking of trip-coil adjustments, operating mechanisms of the oil circuit breakers, air tests on transformer and circuit-breaker tanks, continuity of pilot-wire conductors and secondary mains, and observation of the general condition of the equipment. The pilot cables and leads from current transformers and trip coils are terminated in junction boxes provided with test clips to facilitate testing. The first complete inspection of the oil circuit breakers was made in 1935, and no conditions of distress were found to exist after their several years in service. Continuity of the loop feeders is checked twice weekly by the substation operator opening the breaker on one end of each feeder and noting the change in the ammeter reading on the other end. It was only by this method that many of the primary faults near the midpoint of a loop feeder were indicated, so slight was the disturbance caused by losing only one section of feeder and its associated transformer bank.

Investment and Operating Costs

There are certain inherent economic advantages in the sectionalized loop-feeder system over the conventional radial-feeder system. Since only one unit section with its one associated transformer bank is isolated by a primary-cable or transformer failure, a minimum of additional load is thrown on adjacent transformer banks. This feature, in addition to requiring a minimum of reserve transformer capacity, permits a simplified routing of primary feeders with a consequent saving in the length of primary cable required.

In any final choice between alternative systems, a comparison almost never can be made on the basis of the technical aspects alone; a comparison of the costs must also be given due consideration. Owing to the diversity of conditions met in various localities, it is obviously impossible to make definite statements as to the relative costs of different types of a-c secondary network systems.

From a study of published data on other network systems, however, it is evident that the advantages of the Philadelphia type of network can be obtained with a capital investment no greater than that required for the conventional type of system, and, because of the extreme simplicity of its protective scheme, the operating and maintenance costs are considerably lower. The cost of additional equipment such as pilot cable and primary sectionalizing circuit breakers is amply balanced by savings in other directions, such as the avoidance of the use of expensive and highly sensitive relays and network protectors, and the need for a minimum of spare transformer capacity.

Conclusions

The original design of the Philadelphia a-c network system has long since passed the theoretical or experimental stage. The 12 years' experience in practical application has proved it capable of meeting all the requirements of simplicity, reliability, and economy in the supply of high-grade service to the Philadelphia central district. This commendable record has been obtained from a wide range of operating experiences which have thoroughly tested the system in practice, and conclusively demonstrated the advantages of sectionalized primary loop feeders and a fused secondary network.

News

Of Institute and Related Activities

Winter Convention to Feature Lecture on Atomic Disintegration

A POPULAR lecture on "Atomic Disintegration" by Doctor Enrico Fermi, distinguished physicist and winner of the 1938 Nobel Prize in physics, will be one of the high lights of the Institute's forthcoming winter convention, to be held in New York, N. Y., January 22–26, 1940, with headquarters at the Engineering Societies building. Doctor Fermi's lecture will be delivered on Wednesday evening, January 24, in connection with the presentation of the Edison and Hoover Medals, and will be followed by demonstrations of atomic disintegration by Doctor John R. Dunning, professor of physics, Columbia University.

Atomic disintegration or "fission" by neutron capture is a newly discovered physical phenomenon which may some day be of great significance to engineers on account of the relatively enormous amount of energy liberated in such atomic explosions. These recent experiments mark a long step forward toward the possible utilization on the earth of some of the vast stores of atomic energy now known to furnish the energy of the sun and stars.

Doctor Fermi by his work on the transmutation of atomic nuclei blazed a trail of his own, which led to winning the Nobel Prize. He has been working with Doctor Dunning and the research group in physics at Columbia University using the cyclotron in experiments on the physics of atomic nuclei. Doctor Dunning is a versatile and resourceful demonstrator, who will illustrate by experiments some of the phenomena of which Professor Fermi will speak. The lecture and demonstration will afford an exceptional opportunity for our members to keep abreast of the very latest physical experimentation.

Technical Sessions and Conferences

Nineteen technical sessions have been proposed in 12 different fields of technical-committee activity. These sessions embrace many papers that will present new treatments of existing theories, new designs of electrical apparatus, and the latest in operating practice. The sessions which have been proposed are as follows: transportation (2), industrial power applications (1), electrical machinery (2), production and application of light (1), power generation (1), protective devices (3), power transmission and distribution (2), communication (3), automatic stations and basic sciences (1), electronics (1), instruments and measurements (1), and electric welding (1).

In addition to the 19 technical sessions 5 technical conferences have been proposed as follows: test code for synchronous machines, sound, feedback amplifiers, networks, and increasing the use of electronic devices. These conferences will be informal, some of them conducted as round-table discussions.

Further details of the program will be announced in the January 1940 issue.

Test Code for Synchronous Machines

The technical conference planned on the test code for synchronous machines will take the form of an open meeting of the Institute's subcommittee which has had this code under preparation for some years. Suggestions and comments are cordially invited. It is requested that they be submitted in writing, so far as possible, but opportunity for open discussion will be given, especially on important or controversial points.

Doctor Enrico Fermi, Nobel Prize winner who will speak at the coming AIEE winter convention

The latest edition of the proposed test code was printed in January 1937. Copies may be had upon request to H. E. Farrer at Institute headquarters. Some changes have since been adopted, particularly in the section on synchronous machine quantities (reactances and time constants), to the effect that reactance values determined from three-phase short-circuit tests shall be based upon the a-c components of all three current waves, instead of one symmetrical wave; also, saturation is taken into account by the introduction of "rated current" and "rated voltage" values. A new section on the determination of the synchronizing power constant, *Pr*, is being prepared.

Many other changes have been suggested, not only as to content and language, but as to arrangement, symbols, illustrations, etc. All types of suggestions will be welcomed at the conference, and given consideration if presented in writing. The discussion will be concerned primarily with matters of content and language. Other matters will be heard, so far as time permits. Definite text changes can probably not be adopted at the conference, but will have to be worked out later by the subcommittee, along with editorial details. It is hoped that this can be done in a few months' time, and that the test code can be reported as complete for issue sometime in 1940. All those interested are therefore urged to review the code and take advantage of this opportunity to present their suggestions.

Entertainment

Plans for social events at present include a smoker to be held on Tuesday evening, January 23, and a dinner-dance on Thursday evening, January 25. Special entertainment for women guests will be arranged by the women's entertainment committee, of which Mrs. G. S. Rose is chairman. Inspection trips to places of interest in and about the city will be arranged during the week and on Friday.

Winter Convention Committee

The personnel of the 1940 winter convention committee is as follows: A. F. Dixon, *chairman;* J. W. Barker, T. F. Barton, G. E. Dean, E. E. Dorting, L. C. Miller, A. G. Oehler, J. H. Pilkington, C. S. Purnell, George Sutherland, and W. W. Truran.

Picou Receives Mascart Medal. The Mascart Medal of the Sociètè Française des Électriciens, awarded every three years, has been given in 1939 to R. V. Picou, founder member and honorary president of the society, who is known particularly for his work on magnetism and supplementary poles in machines. The medal, which was established in 1923 in honor of the founder of the society, was last awarded (1936) to the late A. E. Kennelly, honorary member and past president of the AIEE.

CIGRÉ Proceedings. Complete proceedings of the tenth session of the International Conference on High-Voltage Systems (CIGRÉ), held in Paris, France, June 26 to July 9, 1939, will be published in book form early in 1940. They will be issued in French, and, if sufficient advance demand is expressed, in English as well. Copies may be subscribed for in advance by writing Tribot Laspiere, general secretary, CIGRÉ, 54, Avenue Marceau, Paris, France.

AIEE Board of Directors Meets at Institute Headquarters

THE regular meeting of the board of directors of the American Institute of Electrical Engineers was held at Institute headquarters, New York, on October 27, 1939.

There were present: *President*—F. Malcolm Farmer, New York, N. Y. *Past Presidents*—John C. Parker and W. H. Harrison, New York, N. Y. *Vice-Presidents*—T. F. Barton, New York, N. Y.; F. C. Bolton, College Station, Tex.; Chester L. Dawes, Cambridge, Mass.; H. W. Hitchcock, Los Angeles, Calif.; A. H. Lovell, Ann Arbor, Mich.; C. T. Sinclair, Pittsburgh, Pa.; J. M. Thomson, Toronto, Ont.; and Albert L. Turner, Omaha, Nebr. *Directors*—C. R. Beardsley and H. S. Osborne, New York, N. Y.; V. Bush, Washington, D. C.; Mark Eldredge, Memphis, Tenn.; R. E. Hellmund and C. A. Powel, East Pittsburgh, Pa.; F. H. Lane and L. R. Mapes, Chicago, Ill.; K. B. McEachron, Pittsfield, Mass.; F. J. Meyer, Oklahoma City, Okla.; D. C. Prince, Philadelphia, Pa.; and R. W. Sorensen, Pasadena, Calif. *National Treasurer*—W. I. Slichter, New York, N. Y. *National Secretary*—H. H. Henline, New York, N. Y.

Minutes of the meeting of the board of directors held August 4, 1939, and of a special meeting of the executive committee held October 20, 1939, were approved.

Report was made and approved of actions of the executive committee as of September 21, 1939, on applications, as follows: 18 applicants transferred to the grade of Fellow; 23 applicants transferred and 7 elected to the grade of Member; 37 applicants elected to the grade of Associate; 12 Students enrolled.

Reports were presented and approved of meetings of the board of examiners held September 14 and October 19, 1939. Upon recommendation of the board of examiners, the following actions were taken: 1 applicant was reinstated to the grade of Fellow; 9 apolicants were elected to the grade of Member; 42 applicants were elected to the grade of Associate; 763 Students were enrolled.

Monthly disbursements were reported by the finance committee, and approved by the board, as follows: $14,917.03 in August, $16,966.69 in September, and $27,644.55 in October.

Upon recommendation of the finance and Section committees, the following resolution was adopted:

WHEREAS the Board of Directors, on August 8, 1922, authorized Sections' discretion in the expenditure of the Sections' meeting appropriations to cover social activities, and

WHEREAS some Sections have assumed this latitude to cover the payment of additional traveling expenses of delegates to national and District meetings above the amounts provided in the budget, be it

RESOLVED: That the Sections be advised that this practice does not come within the latitude extended the Sections.

A budget for the appropriation year beginning October 1, 1939, was adopted as submitted by the finance committee and revised by the board. (Information about the budget appears elsewhere in this issue.)

The board voted, with regret, to release the Vancouver Section from the responsibility of acting as host for the 1940 Pacific Coast convention, because of the war situation, and accepted the invitation of the Los Angeles Section to hold the convention in Los Angeles or vicinity.

Authorization was given to the Dallas Section for a change in name to "North Texas Section" and an extension of territory.

Upon recommendation of the standards committee, the board approved the appointment of Institute representatives as follows:

R. T. Henry, Stanley Stokes, D. M. Jones, and A. C. Monteith, on the NEMA-EEI-AIEE Joint Committee on Insulation

I. A. Yost on the Sectional Committee on Industrial Lighting, A11, to succeed C. H. More, resigned

C. L. Dawes and W. N. Zippler on the Sectional Committee on Insulated Wires and Cables, C8, to succeed E. B. Meyer, deceased, and Doctor J. B. Whitehead, resigned

Doctor Reinhold Rudenberg on the Sectional Committee on Rotating Electrical Machinery, C50, to succeed Doctor A. E. Kennelly, deceased

D. F. Miner, representative, and A. M. deBellis and A. H. Schirmer, alternates, on the Sectional Committee on Preferred Voltages—100 Volts and Under.

As recommended by the standards committee, the board authorized withdrawal by the AIEE from joint sponsorship with the National Electrical Manufacturers' Association of the Sectional Committee on Electric Welding Apparatus, C52, in favor of sole sponsorship by the American Welding Society.

The resignation of E. E. George as vice-president of the Institute representing the Southern District (4), on account of removal from the District. was accepted with regret, and Professor Fred R. Maxwell, Jr., University of Alabama, Tuscaloosa, Ala., was elected vice-president in his place.

A. H. Kehoe was appointed a representative of the Institute on the standards council of the American Standards Association for the three-year term beginning January 1, 1940, to succeed V. M. Montsinger, whose term will expire at the end of the present calendar year. H. E. Farrer, H. H. Henline, and E. B. Paxton were reappointed alternates for the year 1940.

C. R. Beardsley, C. L. Dawes, Mark Eldredge, A. H. Lovell, and C. A. Powel were elected as the board members to serve on the national nominating committee for the year 1939–40, and T. F. Barton and H. S. Osborne were designated as alternates.

Report was made of notification from The Engineering Foundation of appropriations by Foundation, for the fiscal year 1939–1940, for three research projects sponsored by the AIEE, viz: Stability of Impregnated Paper Insulation, Insulating Oils and Cable Saturants, and work of the Welding Research Committee.

The board accepted an invitation for the AIEE to be represented on the American Research Committee on Grounding, and appointed C. T. Sinclair as the Institute's representative.

At the meeting of the board of directors on May 26, 1939, there was submitted a proposal of the Westinghouse Electric and Manufacturing Company for the establishment, with the income from funds to be donated by the company, of a graduate scholarship to be administered by the AIEE, and to be known as the Charles Le Geyt Fortescue scholarship, as a perpetual memorial to the late Charles Le Geyt Fortescue. The board accepted the proposal and authorized the president to appoint a committee to collaborate with the Westinghouse company in working out the details. The president subsequently appointed a committee, consisting of C. A. Powel (chairman), R. E. Doherty, D. C. Prince, E. B. Roberts, and W. I. Slichter. The committee submitted, at this meeting of the board, a proposed trust agreement and proposed bylaws for the Charles Le Geyt Fortescue scholarship committee, which will make the awards. The board authorized the president and the national secretary to sign the trust agreement, approved the bylaws, and authorized the president to appoint the first committee.

The board authorized an "Address of Greeting" from the Institute to the School of Engineering of Columbia University on the occasion of the 75th anniversary of the School of Engineering.

Report was made of a testimonial to Past-President Charles F. Scott, signed individually by the members of the executive committee of the Institute and included in a bound volume of testimonials, which was presented to Doctor Scott at a dinner given by the Connecticut Section in his honor, on the occasion of his 75th birthday.

It was reported that a scroll of honor, signed by the presidents of several organizations, including the AIEE, was awarded to Dr. Lee de Forest on September 22 at the celebration of de Forest Day at the New York World's Fair.

Other matters were discussed, reference to which may be found in this or future issues of ELECTRICAL ENGINEERING.

Graduate Program at Carnegie Tech. Establishment of a graduate program in electrical engineering at Carnegie Institute of Technology, Pittsburgh, Pa., has been made possible by a grant of $50,000 from the Buhl Foundation, according to recent announcement. Planned to enable trained engineers to engage in industrially useful study and research, the program will be directed by Doctor B. R. Teare, Jr. (A'29, M'36) whose biography appeared in the November issue, page 490. Beginning with the current academic year, the grant will support the graduate courses in electrical engineering for five years.

Future AIEE Meetings

Winter Convention
New York, N. Y., January 22–26, 1940

Summer Convention
Swampscott, Mass., June 24–28, 1940

Pacific Coast Convention
Place and date to be announced

The Institute Budget for the Year 1939–40

AN INCREASE in the number of pages of technical papers and discussions in the monthly journal, ELECTRICAL ENGINEERING, and in the 1939 annual bound volume of TRANSACTIONS—an increase in the rate of traveling expenses for delegates to Institute meetings and to the District conferences for which such allowances are granted (from $7^{1}/_{2}$¢ to $8^{1}/_{2}$¢ per mile one way) effective January 1, 1940—the addition of one person to headquarters' staff to assist in the work of the standards and technical program committees—an initial payment of $5,000 to a pension fund reserve—are provided for in the budget of income and expenditures for the appropriation year beginning October 1, 1939, which the board of directors adopted at its meeting on October 27, 1939.

In making its budget recommendations to the board the finance committee endeavored, with respect to the estimate of income, to allow for a probable loss of dues revenue which will result from the conditions existing in those countries involved in the present war. The expenditure budget was also presented after careful study of the financial requirements for the year as estimated by the standing committees. As a result of modifications made at the meeting of the board of directors, for the time being the budget is slightly out of balance in that the budgeted expenditures exceed the anticipated income for the year by about $2,000. The finance committee, however, will be prepared at the January meeting of the board of directors to recommend adjustments of the budget to bring it in balance.

A detailed tabulation of the budget appears herewith. From this it will be seen that practically all activities are being afforded at least as much financial support as last year; in some cases an increased appropriation is being granted to cover an enlarged program of activities.

The July issue of ELECTRICAL ENGINEERING carried in the news section (pages 305–317) the complete report of the Institute board of directors for the fiscal year ending on April 30, together with financial statements for the corresponding period. In this report will be found an extensive statement covering such activities as national and District meetings of the Institute, Section and Branch activities, etc., making it unnecessary to repeat at this time the details underlying the appropriations for the principal activities. Should further information be desired, this can be obtained upon correspondence with Institute headquarters.

Each year the board of directors endeavors to make sure that the budget adopted will make possible a proper relative emphasis on the different phases of Institute activities and will limit the annual expenditures to the amount of anticipated income. Membership dues, of course, comprise the principal source of Institute revenue, so that the success of all work planned for in the budget depends largely upon the prompt collection of dues.

Institute Income and Expenses for Year Ending September 30, 1939, and Budget for Year Ending September 30, 1940

	Actual Income and Expenses, Year Ending 9-30-39	Budget for Year Ending 9-30-40
Income		
Dues	$198.745.84	$201,000.00
Students' fees	12,677.50	13,000.00
Entrance fees	6,411.49	6,800.00
Transfer fees	950.00	1,000.00
Advertising	28,641.02	30,000.00
ELEC. ENGG.—non-mem. subscriptions	15,295.51	14,000.00
TRANS. subscriptions	7,442.71	7,300.00
Miscellaneous sales	12,790.99	10,300.00
Badge sales	1,768.00	1,800.00
Interest on securities	7,755.49	6,800.00
Total	$292,478.55	$292,000.00
Expenses		
Publications:		
Text matter (ELEC. ENGG. & TRANS.)	†$66,121.24	*$75,170.00
TRANS. SUPPLEMENT	195.41	350.00
Preprints	8,940.00	8,800.00
Advertising section	14,304.62	15,600.00
Year Book	6,482.67	6,750.00
Miscellaneous expense	1,310.93	1,900.00
Institute meetings	13,726.16	14,750.00
Institute Sections:		
Appropriations	23,376.17	24,300.00
Other expenses	5,783.03	5,950.00
Institute Branches:		
Meetings expenses	1,058.06	1,100.00
Other expenses	2,077.16	2,500.00
Committees:		
Code of prin. prof. conduct		50.00
Edison Medal	146.61	150.00
Finance	1,600.00	800.00
Headquarters	202.10	300.00
Lamme Medal	30.57	160.00
Legislation		50.00
Membership	7,770.94	8,350.00
Model registration Law	187.53	
Standards	6,542.60	8,300.00
Technical	271.79	300.00
Traveling Expenses:		
Geo. Dist. exec. committees	3,229.29	3,000.00
Section delegates to summer conv.	11,769.93	6,500.00
Counselor delegates to summer conv.	1,393.89	1,250.00
Dist. secys. to summer conv.	1,021.65	1,150.00
District Student conferences	6,066.85	6,500.00
President's appropriation		$ 1,500.00
Vice-presidents	435.37	570.00
Board of directors	5,861.47	5,900.00
National nominating committee	1,020.79	1,200.00
AIEE representatives		100.00
Administration:		
Headquarters' salaries	35,311.50	36,700.00
Postage	3,887.47	4,000.00
Stationery & printing	3,372.82	3,700.00
Office equipment	342.71	400.00
Trav. expense, insurance, misc. supplies & services	3,096.72	3,275.00
Paper prizes	366.80	515.00
Joint activities:		
American Engineering Council	9,000.00	6,000.00
American Standards Assn.	1,500.00	1,500.00
Engrs. Council Prof. Dev.	850.00	850.00
Engg. Foundation research proj.:		
Impregnated paper insulation	250.00	250.00
Insulating oils & cable saturants		250.00
Welding	250.00	250.00
Engg. Soc. Employment Service	1,762.92	1,108.00
Engg. Soc. Library	9,689.30	9,900.00
Hoover Medal		150.00
John Fritz Medal	251.92	....
United Engg. Trustees building assessment	10,984.80	10,985.00
U. S. Natl. Com. I. C. I.	300.00	300.00
Miscellaneous printing, etc.		3,400.00
Authors' reprints	2.231.67	....
Reprints of standards	758.28	....
TRANSACTIONS Index	3,855.10	....
Miscellaneous	80.09	....
Other expenses		
Membership badges	1,244.04	1,750.00
Text paper & env. in storage	844.39	....
Pension Fund Reserve		5,000.00
Miscellaneous	708.28	310.00
Total	$281,865.64	$293,893.00

† Actual expense $68,626.90, including paper costs paid for previously.

* Actual budget of $75,870.00 reduced by text paper inventory of $700.00

Executive Committee of AIEE District 1 Meets

The executive committee of the AIEE North Eastern District met October 13, 1939, at Schenectady, N. Y. Those present were:

C. L. Dawes, vice-president, North Eastern District
R. G. Lorraine, secretary, North Eastern District
E. M. Strong, chairman, District committee on Student Branches
G. W. Dunlap, representing District membership committee
A. C. Stevens, past vice-president, North Eastern District
C. W. LaPierre, chairman, Schenectady Section
E. M. Hunter, vice-chairman, Schenectady Section
C. W. Henderson, chairman, Syracuse Section
True McLean, chairman, Ithaca Section
J. P. Wood, secretary-treasurer, Ithaca Section
C. F. Savage, chairman, Lynn Section
R. G. Porter, representing executive committee of Boston Section
M. E. Scoville, secretary-treasurer, Pittsfield Section
J. H. Milbyer, secretary-treasurer, Niagara Frontier Section
V. Siegfried, representing chairman of Worcester Section
R. S. Judd, chairman, Connecticut Section
A. G. Conrad, secretary-treasurer, Connecticut Section
W. B. Hall, chairman, Providence Section
W. F. Cotter, vice-chairman and secretary, Rochester Section
H. D. Griffith, secretary, Springfield Section
K. B. McEachron, AIEE Director
H. H. Race, chairman, AIEE Sections committee

Vice-President Dawes presided. On behalf of the general convention committee for the 1940 summer convention, two meetings were reported, and the formation of working subcommittees on entertainment and banquets, trips, hotels and registration, transportation, sports, student activities, publicity, women's entertainment, finance, general arrangements and co-ordination.

All these committees have reported preliminary plans and budgets.

The invitation of the Rochester Section for a District meeting to be held in Rochester in 1941 was accepted by the committee. Plans for having President Farmer visit the various Sections of the District were discussed and joint meetings considered.

The committee nominated E. S. Lee (A'20, F'30) Schenectady, N. Y., for AIEE vice-president, District 1, for 1940–41. W. B. Hall was elected District representative on the national nominating committee. Suggestions for Institute president or director are to be sent by the District executive committee to the District vice-president and District secretary.

The following were appointed to the District co-ordinating committee, in addition to the vice-president, secretary, and chairman of the committee on Student Branches:

R. H. Bryant
C. F. Savage
J. C. Balsbaugh

The following were appointed judges for the District prize awards for papers presented during 1938–39:

C. W. LaPierre
J. G. Patterson
H. M. Turner

Chairman Race of the AIEE Sections committee, emphasizing the importance of Section activities in the progress of the Institute, suggested in addition to the activities already carried on by most Sections (see report, *EE, Oct. '39, pp. 436–7*) the following:

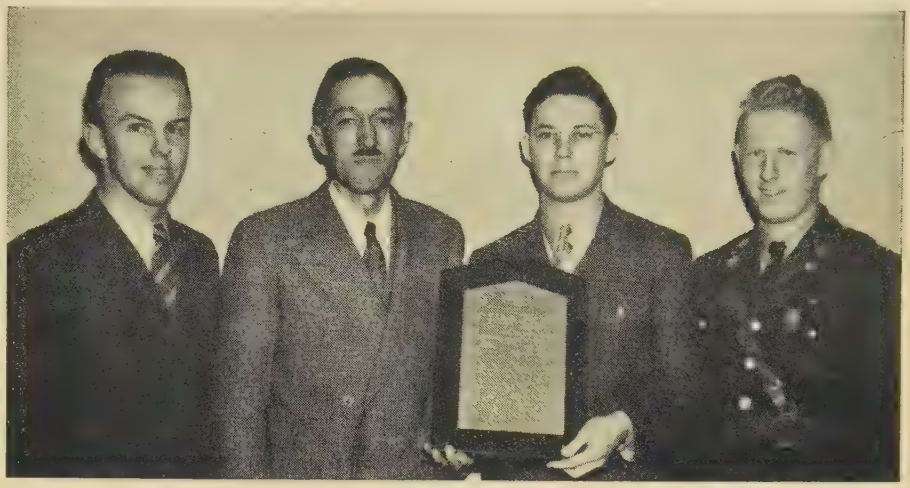
1. Appointment of a committee on legislation, either alone or in co-operation with other local engineering organizations.
2. Appointment of a committee on economic and social affairs, alone or co-operatively, to stimulate and organize group interest in such considerations.
3. Appointment of a committee on vocational guidance to provide speakers and distribute material on the engineering profession.
4. Stimulation of active participation by younger members, through follow-up of student members moving into Section territory, and by giving young men in the Section opportunities for active Section work.

Doctor Race announced two booklets to be available from Institute headquarters soon: one on "Section Activities," and one, called "The Electrical Engineer," which will provide preparatory school students and vocational counselors with information about the profession. This booklet and the ECPD publication, "Engineering—a Career, a Culture," were suggested for the use of Section vocational guidance committees.

Membership activities, District financial matters, and other topics were also discussed at the meeting.

Doctor Bláthy Dies. Doctor Otto Titus Bláthy, Hungarian inventor and electrical engineer, died at Budapest, Hungary, September 26, 1939. Born in 1860, he had been employed since 1883 by the electrotechnical factory of Ganz and Company, Budapest. He made a number of contributions to electrical development during the early history of the art, and continued to perform valuable work until the time of his death. He had received various honors, both in his own and other countries.

AIEE Minnesota Branch Given "Best Society" Award



The AIEE Student Branch at the University of Minnesota, Minneapolis, received the Best Society Award presented by the "Tech Commission" to the most outstanding student organization in the Institute of Technology during the school year 1938–39. The award is made on the basis of a point system: The percentage of eligible students who are members of the society, minus a "handicap" calculated on the basis of past membership records, determines membership points; activity points ranging in number from 2 to 20 are given for such activities as open house, electrical-engineering show, banquets, field trips, meetings, floats, field-day participation, cup, social gatherings, and others. Membership points multiplied by 0.4, plus activity points multiplied by 0.6, give the organization's final score, which for the AIEE Branch was 93.2. Current officers of the Branch shown here with the Best Society plaque are, left to right, Robert Lyons, vice-chairman; Professor J. H. Kuhlmann, counselor; Robert MacDonald, chairman (holding plaque); Elmer Brickman, secretary-treasurer.

Dallas Section Becomes North Texas Section

By action of the AIEE board of directors on October 27, 1939, the name of the Dallas Section has been changed to North Texas Section. The Section's territory has been extended to include the area west and northwest of the present Section territory, comprising 133 counties in all.

The Dallas Section was organized May 18, 1928. Its officers for 1939–40 are C. W. Mier (A'25, M'31) engineer, Southwestern Bell Telephone Company, Dallas, *chairman;* H. R. Pearson (A'36) engineer, Dallas Power and Light Company, Dallas, *secretary.*

AIEE National and District Prizes for Papers

Provision in the Institute's 1939–40 budget has been made for cash awards to accompany the national and District prizes for initial and Branch papers to be awarded during the current year, as indicated below.

All technical papers presented before the Institute during the year are eligible under the AIEE paper prize regulations for competitive consideration for one or more of the established prizes, regardless of whether presented before a Branch meeting, a Section meeting, a District meeting, or a national convention, the several classes providing for equitable competition. With the exception of the prizes for Branch papers, both District and national prizes are awarded each spring for papers presented during the preceding calendar year. For Branch papers, both national and District prizes are awarded on the basis of the academic year, July 1 to June 30, inclusive.

National prizes that may be awarded annually, and those to be accompanied by cash awards during the current year, are as follows:

1. Best paper prizes (certificates):
   Engineering practice
   Theory and research
   Public relations and education
2. Prize for initial paper ($100 and certificate)
3. Prize for Branch paper ($100 and certificate)

District prizes that may be awarded annually, together with the cash awards provided for the current year, are:

1. Prize for best paper (certificate)
2. Prize for initial paper ($25 and certificate)
3. Prize for Branch paper ($25 and certificate)
4. Prize for graduate student paper (certificate)

Although all papers presented are *eligible,* there is no provision for automatic consideration of papers, except that those approved by the technical program committee and presented at national conventions or District meetings will be considered by the national prize committee for the national "best paper" and the "initial paper" prizes without being offered formally for competition. All other papers for which prize consideration is desired must be *submitted*

specifically for that purpose, through the District secretary for District prizes, or through the national secretary's office for the national prizes.

Deadline Dates

Papers to be considered for 1939 national prizes must be submitted not later than February 15, 1940. Papers to be considered for the District "best" and "initial" paper prizes must be submitted on or before February 15, 1940; for the District, Branch, and graduate student paper prizes, before July 15, 1940 (for papers presented during the academic year ending June 30, 1940). Those wishing further information may obtain a booklet entitled "National and District Prizes" from AIEE Headquarters, 33 West 39th Street, New York, N. Y.

John Fritz Medal Awarded to Late C. F. Hirshfeld

The John Fritz Gold Medal for 1940 has been awarded posthumously to Doctor C. F. Hirshfeld (A'05, F'36), by unanimous vote of the board of award. He was under consideration for the medal at the time of his death, April 19, 1939. The award, the highest distinction bestowed jointly by the four national societies of civil, mining and metallurgical, mechanical, and electrical engineers, for notable scientific and industrial achievement, went to Doctor Hirshfeld "for notable leadership through research and development in power generation and electric traction, and for being a great teacher and friend of men both old and young."

Doctor Hirshfeld was chief of the department of research of the Detroit Edison Company, Detroit, Mich. A biographical sketch and obituary notice appeared in the May issue, page 229.

Award of the John Fritz Medal is made not oftener than once a year, without restriction on account of nationality or sex. Each participating society has four representatives on the board of award. AIEE members of the present board, all past presidents, are: J. B. Whitehead (A'00, F'13), A. M. MacCutcheon (A'12, F'26), W. H. Harrison (A'20, F'31), and John C. Parker (A'04, F'12).

W. F. Davidson Heads NRC Insulation Conference

At its 12th annual meeting, held November 2–4, 1939, at Harvard University, Cambridge, Mass., the National Research Council's conference on electrical insulation took on a new pilot—Ward F. Davidson (A'14, F'26) director of research for the Consolidated Edison Company of New York and chairman of the AIEE committee on research. Originally organized in 1928 as the NRC committee on electrical insulation, the conference with two exceptions has been operating continuously under the stimulating leadership of its retiring chairman, Doctor John B. Whitehead (A'00, F'12) director of The Johns Hopkins University school of engineering. The exceptions noted were the period (1933–34) of Doctor Whitehead's presidency of the AIEE, and the last half of the current year. A serious illness in the Spring of 1939 and an oft-expressed belief in the desirability of having "new blood," caused Doctor Whitehead to insist upon the acceptance of his resignation from the chairmanship. During these interim periods, the responsibilities of leadership fell upon and were assumed by Vice-Chairman William A. Del Mar (A'06, F'20) chief engineer of the Habirshaw Cable and Wire Division of the Phelps Dodge Copper Products Corporation and long-time active participant in AIEE affairs.

At its annual dinner meeting November 3, the conference adopted by acclaim a citation paying fitting tribute to retiring Chairman Whitehead for his years of devoted service. The citation follows:

"The conference on electrical insulation now holding its 12th annual meeting, finds itself to be an organization without parallel in the field of science and engineering. Nowhere else do we find a group of research men meeting to discuss informally work in progress in their respective laboratories. The atmosphere of mutual trust and enthusiastic co-operation is unique not only in this country but, as far as we know, in the world.

"Where such a phenomenon occurs in any field, its existence is traceable to a rare personality. In this case the personality is that of our beloved retiring chairman, Doctor J. B. Whitehead.

"The conference extends its appreciation and thanks for the spirit that Doctor Whitehead has infused into the conference, and for the immense amount of labor he has devoted to its successful development since its inception. May he continue to be an active member for the years to come."

In his brief response, Doctor Whitehead insisted that the evergrowing success of the conference's work should be attributed to the effective co-operation of the many participants.

With the new appointments and holdover officers, the executive committee of the conference now is constituted as follows:

W. F. Davidson, Consolidated Edison Company, New York, N. Y., chairman

S. O. Morgan, Bell Telephone Laboratories, New York, vice-chairman

Thorstein Larsen, Consolidated Edison Company, New York, secretary

H. H. Race, General Electric Company, Schenectady, N. Y., chairman, committee on physics

Arthur von Hipple, Massachusetts Institute of Technology, Cambridge, vice-chairman, committee on physics

E. L. Miller, Esso Laboratories, Elizabeth, N. J., chairman, committee on chemistry

R. N. Evans, Consolidated Edison Company, New York, vice-chairman, committee on chemistry

C. F. Hill, Westinghouse Electric and Manufacturing Co., East Pittsburgh, Pa., chairman, committee on monographs

C. L. Dawes, Harvard University, Cambridge, Mass.

W. A. Del Mar, Phelps Dodge Copper Products Corporation, Yonkers, N. Y.

J. B. Whitehead, The Johns Hopkins University, Baltimore, Md.

Pursuing its customary procedure, the conference at its Cambridge sessions accommodated some two dozen informal presentations of the general nature of progress reports reflecting for the most part the status of current research in what appears to be an ever-widening field directly contributory to better electrical insulating materials and a better understanding of them. A comprehensive report reflecting the highlights of the reports presented is in preparation and scheduled for inclusion in an early issue of Electrical Engineering.

Registered attendance at the Cambridge sessions was 131.

Technological Institute At Northwestern

Classes began this fall in the new Technological Institute established at Northwestern University, Evanston, Ill., by a $6,735,000 gift from the Walter P. Murphy Foundation, Chicago. The Institute, which will train a student body of 800 in civil, mechanical, electrical, and chemical engineering, is headed by Dean Ovid W. Eshbach (A'17, F'37), and will be housed in a new building, to be begun early in 1940. The engineering faculty of the University has been increased to take care of the Institute students. Courses, which are on a five-year basis, are arranged on the co-operative plan of engineering education developed by the late Dean Herman Schneider at the University of Cincinnati, under which students will devote alternate quarters of their second, third, and fourth years to classes and to field jobs in co-operating industries. The class of 100 freshmen that entered the Institute at the beginning of the 1939–40 term represents a selected group, 73 per cent of which ranked in the upper quarter of their high-school classes. Dean R. C. Disque (M'20) of the school of engineering, Drexel Institute of Technology, Philadelphia, Pa., is acting as educational consultant for Northwestern's new Institute.

The building that is to house the Institute will be the largest on the Northwestern campus, and will contain the chemistry and physics departments of the University, as well as the engineering departments. It is expected to be ready for classes by September 1941. The building, which will cost $4,920,000, will require 4,564,290 man-hours of work in shop and field, in 32 industries, according to estimates.

Engineering School Anniversary at Columbia

With a variety of activities, including a three-day open house at scientific and engineering laboratories, demonstrations of recent developments in research and instruction, a series of lectures on strategic minerals, dedication of a memorial plaque, and awards of medals, the school of engineering of Columbia University, New York, N. Y., during November brought to a close a ten-month celebration of its 75th anniversary.

Development of the Columbia school of engineering began with the establishment in 1864 of the school of mines, said to be the first in the United States. Provision for civil engineering was made in 1868, and courses in metallurgical, electrical, mechanical, chemical, and industrial engineering and in mineral dressing were established later. The departments which conduct these courses now constitute the school of engineering.

List of Undergraduate Curricula Accredited by ECPD as of October 20, 1939

(Subject to continual revision. For basis of accrediting see ELECTRICAL ENGINEERING, December 1938, page 515; for report of ECPD seventh annual meeting, see November 1939 issue, page 485.)

University of Alabama: Aeronautical, civil, electrical, industrial, mechanical, mining

Alabama Polytechnic Institute: Electrical, mechanical

University of Arizona: Civil, electrical, mechanical, mining

University of Arkansas: Civil, electrical, mechanical

Armour Institute of Technology: Chemical, civil, electrical, mechanical

Brooklyn Polytechnic Institute: Chemical (day and 8-year evening), civil (*a*), electrical (*a*), mechanical (*a*)

Brown University: Civil, electrical, mechanical

Bucknell University: Civil, electrical, mechanical

University of California: Civil, electrical, mechanical, metallurgical (metallurgy), mining, petroleum

California Institute of Technology: Aeronautical (6-year course), chemical (5-year course), civil, electrical, mechanical

Carnegie Institute of Technology: Chemical, civil (*a*), electrical (*a*), industrial (management) (*a*), mechanical (*a*), metallurgical (*a*)

Case School of Applied Science: Chemical, civil, electrical, mechanical, metallurgical

Catholic University of America: Aeronautical, architectural, civil, electrical, mechanical

University of Cincinnati: Aeronautical, chemical, civil, electrical, mechanical

The Citadel: Civil

Clarkson College of Technology: Chemical, civil, electrical, mechanical

Clemson Agricultural College: Civil, electrical, mechanical

College of the City of New York (*a*): Civil, electrical, mechanical

University of Colorado: Architectural, civil, electrical, mechanical

Colorado School of Mines: Geological, metallurgical, mining, petroleum

Colorado State College: Civil, electrical, mechanical

Columbia University (*b*): Chemical, civil, electrical, industrial, mechanical, metallurgical, mining

Cooper Union Institute of Technology (*c*): Civil, electrical, mechanical

Cornell University: Chemical, civil, electrical, industrial (administrative), mechanical

Dartmouth College: Civil

University of Delaware: Civil, electrical, mechanical

University of Detroit: Aeronautical, architectural, civil, electrical, mechanical

Drexel Institute: Chemical, civil, electrical, mechanical

Duke University: Civil, electrical, mechanical

University of Florida: Civil, electrical, industrial, mechanical

Georgia School of Technology: Aeronautical, chemical (excluding co-operative curriculum), civil, electrical, mechanical

Harvard University (*d*): Civil, communication, electrical, industrial (engineering and business administration), mechanical, metallurgical (physical metallurgy), sanitary

University of Idaho: Civil, electrical, mechanical, metallurgical (metallurgy), mining

University of Illinois: Architectural, ceramic (technical option), chemical, civil, railway civil, electrical, railway electrical, general (*e*), mechanical, railway mechanical, metallurgical, mining

Iowa State College: Agricultural, architectural, chemical, civil, electrical, general (*e*), mechanical

State University of Iowa: Chemical, civil, electrical, mechanical

Johns Hopkins University: Civil, electrical, mechanical

University of Kansas: Architectural, civil, electrical, mechanical, mining

Kansas Stage College: Agricultural architectural, civil, electrical, mechanical

University of Kentucky: Civil, metallurgical, mining

Lafayette College: Civil, electrical, industrial (administrative), mechanical, metallurgical, mining

Lehigh University: Chemical, civil, electrical, industrial, mechanical, metallurgical, mining

Louisiana State University: Chemical, civil, electrical, mechanical, petroleum

University of Louisville: Chemical, civil, electrical, mechanical

University of Maine: Civil, electrical general (*e*), mechanical

Marquette University: Civil, electrical, mechanical

University of Maryland: Civil, electrical, mechanical

Massachusetts Institute of Technology: Aeronautical, building engineering and construction, chemical, civil, electrical, electrochemical, general (*e*), industrial (business and engineering administration), mechanical, metallurgical (metallurgy), mining, naval architecture and marine engineering (including marine transportation), public health, sanitary

University of Michigan: Aeronautical, chemical, civil, electrical, engineering mechanics, mechanical, metallurgical, naval architecture and marine engineering, transportation

Michigan College of Mining and Technology: Civil, electrical, mechanical, metallurgical, mining

Michigan State College: Civil, electrical, mechanical

University of Minnesota: Aeronautical, chemical, civil, electrical, mechanical, metallurgical, mining, petroleum

University of Missouri: Civil, electrical, mechanical

Missouri School of Mines and Metallurgy: Ceramic, civil, electrical, metallurgical, mining (mine) (including petroleum option)

Montana School of Mines: Geological, metallurgical, mining

Montana State College: Civil, electrical, mechanical

University of Nebraska: Agricultural, architectural, civil, electrical, mechanical

University of Nevada: Electrical, mechanical, mining

University of New Hampshire: Civil, electrical, mechanical

University of New Mexico: Civil, electrical, mechanical

New Mexico State College: Civil, electrical, mechanical

New York University: Aeronautical, chemical (day and 7-year evening), civil (*a*), electrical (*a*), mechanical (*a*)

New York State College of Ceramics (at Alfred University): Ceramic

Newark College of Engineering: Civil, electrical, mechanical

North Carolina State College: Ceramic, civil, electrical, mechanical

University of North Dakota: Chemical, civil, electrical, mechanical, mining

Northeastern University: Civil, electrical, industrial, mechanical

Northwestern University: Civil, electrical, mechanical

Norwich University: Civil, electrical

Ohio State University: Ceramic, chemical, civil, electrical, industrial, mechanical, metallurgical, mining (mine)

University of Oklahoma: Architectural, civil, electrical, mechanical, petroleum (production option)

Oklahoma Agricultural and Mechanical College: Civil, electrical, industrial, mechanical

Oregon State College: Civil, electrical, mechanical

University of Pennsylvania: Chemical, civil, electrical, mechanical

Pennsylvania State College: Architectural, ceramic (ceramics), chemical, civil, electrical, electrochemical, fuel technology, industrial, mechanical, metallurgical (metallurgy), mining, petroleum and natural gas, sanitary

University of Pittsburgh: Chemical, civil, electrical, industrial, mechanical, metallurgical, mining, petroleum

Pratt Institute: Electrical, mechanical

Princeton University: Chemical, civil, electrical, mechanical

Purdue University: Chemical, civil, electrical, mechanical

Rensselaer Polytechnic Institute: Aeronautical, chemical, civil, electrical, industrial, mechanical, metallurgical

Rhode Island State College: Civil, electrical, mechanical

Rice Institute: Civil, electrical, mechanical

University of Rochester: Mechanical

Rose Polytechnic Institute: Civil, electrical, mechanical

Rutgers University: Civil, electrical, mechanical, sanitary

University of Santa Clara: Civil, electrical, mechanical

South Dakota State College: Civil, electrical, mechanical

South Dakota State School of Mines: Civil, electrical, metallurgical, mining

University of Southern California: Petroleum

Southern Methodist University: Civil, electrical, mechanical

Stanford University: Civil, electrical, mechanical, metallurgical, mining, petroleum

Stevens Institute of Technology (*e*): General

Swarthmore College: Civil, electrical, mechanical

Syracuse University: Civil, electrical, industrial (administrative), mechanical

University of Tennessee: Chemical, civil, electrical, mechanical

University of Texas: Architectural, civil, electrical, mechanical, petroleum (petroleum production)

Agricultural and Mechanical College of Texas: Civil, electrical, mechanical, petroleum

Texas Technological College: Civil, electrical, mechanical

Tufts College: Civil, electrical, mechanical

Tulane University of Louisiana: Civil, electrical, mechanical

University of Tulsa: Petroleum (including options in refining and production)

Union College: Civil, electrical

United States Coast Guard Academy (*e*): General

University of Utah: Civil, electrical, mechanical, metallurgical, mining

Utah State Agricultural College: Civil

Vanderbilt University: Civil, electrical, mechanical

University of Vermont: Electrical

University of Virginia: Civil, electrical, mechanical

Virginia Military Institute: Civil, electrical

Virginia Polytechnic Institute: Ceramic, chemical, civil, electrical, industrial, mechanical

Washington University: Architectural, civil, electrical, industrial (administrative), mechanical

University of Washington: Aeronautical, ceramic, chemical, civil, electrical, mechanical, metallurgical, mining

State College of Washington: Architectural, civil, electrical (basic and hydroelectric options), mechanical (basic option), metallurgical, mining

Webb Institute of Naval Architecture: Naval architecture and marine engineering

West Virginia University: Civil, electrical, mechanical, mining

University of Wisconsin: Chemical, civil, electrical, mechanical, metallurgical, mining

Worcester Polytechnic Institute: Civil, electrical, mechanical

Yale University: Chemical, civil, electrical, mechanical, metallurgical (metallurgy)

(*a*). Accrediting applies to both the day and evening curricula.

(*b*). Accrediting applies to the 4-year and 5-year curricula leading to the bachelor of science degree.

(*c*). Accrediting applies to day curriculum only. Action on evening curriculum deferred pending granting of degrees.

(*d*). Accrediting applies only to curriculum as submitted to ECPD and upon completion of which a certificate is issued by Harvard University certifying that the student has pursued such a curriculum.

(*e*). The accrediting of a curriculum in general engineering implies satisfactory training in engineering sciences and in the basic subjects pertaining to several fields of engineering; it does not imply the accrediting, as separate curricula, of those component portions of the curriculum such as civil, mechanical, or electrical engineering that are usually offered as complete professional curricula leading to degrees in these particular fields.

Future Meetings of Other Societies

American Association for the Advancement of Science. Winter meeting, December 27, 1939–January 2, 1940, Columbus, Ohio.

American Institute of Mining and Metallurgical Engineers. 152d annual meeting, February 12–15, 1940, New York, N. Y.

American Physical Society. Annual meeting (232d), December 28–30, Columbus, Ohio.

233d meeting, February 22–24, 1940, New York, N. Y.

American Society of Civil Engineers. Annual meeting, January 17–20, 1940, New York, N. Y.

American Society of Heating and Ventilating Engineers. 46th annual meeting, January 22–26, 1940, Cleveland, Ohio.

American Society of Refrigerating Engineers. Annual meeting, January 17–19, 1940, Chicago, Ill.

Louisiana Engineering Society. Annual meeting, January 12–13, 1940. New Orleans, La.

Society of Automotive Engineers. Annual meeting, January 15–19, 1940, Detroit, Mich.

New Engineering Degree to Be Given at NYU

The degree of doctor of engineering science will be conferred by the graduate division of the New York University College of Engineering, New York, N. Y., at the close of the academic year 1939–40. The new degree, the first of its type to be offered by an accredited engineering school in the United States, is approved by the Council of the University and the New York State Board of Regents.

A co-operative plan for graduate instruction in electrical engineering in which New York University, Brooklyn Polytechnic Institute, Brooklyn, N. Y., Stevens Institute of Technology, Hoboken, N. J., and the educational division of Westinghouse Electric and Manufacturing Company, New York, will participate, has also been announced. This is the first such program of graduate study to be put into practice in the New York City area.

Illinois Institute of Technology. Armour Institute of Technology, and Lewis Institute, both in Chicago, Ill., are to be consolidated into a new and larger institution, following an agreement by the trustees of the two schools. The new school will be known as Illinois Institute of Technology, and the names of the two component schools will be retained for its two divisions. Actual consolidation of the educational program will be complete by September 1940. For the present, the buildings and equipment of both Armour and Lewis will be used. A board of 55 trustees, made up of the present trustees of both, will govern the new institution, which will have an enrollment of about 7,000 day and evening students.

Vermilye Medal Established by Franklin Institute

The Franklin Institute has awarded the first Vermilye Medal, presented "in recognition of outstanding contribution in the field of industrial management," to Lewis H. Brown, president, Johns-Manville Corporation, New York, N. Y. The medal, named for its donor, W. M. Vermilye, vice-president, National City Bank of New York, will be awarded biennially or at longer intervals by the Franklin Institute, on recommendation of a committee of board members and an advisory committee including engineers and industrialists and representatives of The American Society of Mechanical Engineers, the Chamber of Commerce of the United States, and the National Association of Manufacturers. The last-named is represented by J. W. O'Leary (A'18) chairman of the board, Arthur J. O'Leary and Son Company, Chicago, Ill.

The presentation to Mr. Brown, "for his brilliant work in executive management in industry," was made at Philadelphia, Pa., November 14, 1939.

Fifty Years of Meters Commemorated by G.E.

Fifty years of manufacturing watt-hour meters at the West Lynn, Mass., plant of the General Electric Company was celebrated October 4, 1939, with a program at the Lynn municipal stadium, open house at the plant, a commemorative dinner, and other events. The 20,000,000th G.E. watt-hour meter was presented to Alex Dow (A'93, F'13, HM'37) president of the Detroit Edison Company, Detroit, Mich., who was chairman in 1898 of the original meter committee of the Association of Edison Illuminating Companies.

The Thomson-Houston Electric Company, one of the predecessors of General Electric, began in 1889 to manufacture the watt-hour meter developed by Elihu Thomson (A'84, F'13, HM'28) co-founder of the company. In fundamentals, this type of instrument has not been materially changed since it was sent by Thomson to the Paris Exposition where it divided first prize. It has long been the standard meter manufactured by General Electric for all d-c uses, though superseded for a-c use by the induction wattmeter.

New Boulder Dam Generator. The eighth generator of 82,500-kva capacity has gone into operation at Boulder Dam, serving the Southern California Edison Company, Ltd. The new unit is the third generator operating in the Arizona wing of the powerhouse. Another large generator and a small one were already in operation on that side, and six large ones on the Nevada side. Like its immediate predecessor, installation of which was noted in the October issue, page 438, the new unit was hurried to completion to meet a power shortage resulting from insufficient rain. It brings the installation to about half its final capacity.

Thurston Book Reissued by Cornell

"A History of the Growth of the Steam-Engine," has been reissued in a "centennial edition" by Cornell University, in connection with its centennial celebration of the birth of the author, Robert H. Thurston, who was director of the Sibley College of Mechanical Engineering at Cornell University from 1885 to 1903. A prefatory statement prepared by Professor William N. Barnard, present director of the Sibley School of Mechanical Engineering at Cornell University, calls attention to the fact that Doctor Thurston's original book issued in 1878:

> "Met the need for such a work in so satisfactory a manner that the book passed through six editions, the last published in 1907 with an additional chapter. Even now, despite the appearance of later books on the same subject by other authors, his presentation is considered a classic for the period of history covered, and there is still considerable demand for it, with but few copies readily accessible. Accordingly, it seems in every way appropriate to issue a new edition of this work, and to do so in connection with the one hundredth anniversary of the author's birth, celebrated at Cornell on October 25, 1939."

Inasmuch as Doctor Thurston's own story ends with the last century, a supplementary chapter by Professer Barnard has been added to the centennial edition, tracing some of the more important developments in steam-power engineering since that time. Copies of the centennial edition, containing 555 $5^1/_2$-by-8-inch pages, may be procured from the Cornell University Press, 124 Roberts Place, Ithaca, N. Y., at $3.00 each.

New ASME Officers. New officers of The American Society of Mechanical Engineers, installed at the annual meeting of the society, Philadelphia, Pa., December 4–8, 1939, are: *president*—W. H. McBryde, San Francisco, Calif.; *vice-presidents*—K. H. Condit, New York, N. Y., Francis Hodgkinson (A'02), New York, N. Y., J. C. Hunsaker, Cambridge, Mass., K. M. Irwin, Philadelphia, Pa.; *managers*—J. W. Eshelman, Birmingham, Ala., Linn Helander, Manhattan, Kans., G. T. Shoemaker (M'20), Chicago, Ill.

"Results of Municipal Lighting Plants," sixth edition has just been issued by Burns and McDonnell Engineering Company of Kansas City, Mo.; price, $5.00. Flexibly bound and containing 380 pages, including several charts and graphs, this book is introduced as presenting the latest available information from 717 municipal lighting plants, in the United States, Canada, Alaska, and the Philippines, concerning rates, earnings, cost of production, revenues, and other pertinent information.

Fifty-Year Employees Honored. Allis-Chalmers Manufacturing Company, Milwaukee, Wis., recently honored 38 men employed by the company 50 or more years by issuing a booklet "We've Gone Places Together," which contained photographs and biographies of the 50-year men. Copies of the booklet, and gold watches, were presented to the men at a dinner in their honor September 23, 1939.

NAM to Honor Modern American Pioneers

"Modern Pioneers," defined as the inventors who have contributed most to the American standard of living during the past 25 years, will be honored by the National Association of Manufacturers at a celebration commemorating the 150th anniversary of the establishment of the United States patent system. A committee of six scientists, of which Doctor K. T. Compton (F'31) president of Massachusetts Institute of Technology, Cambridge, is chairman, has been chosen to select the inventors and scientists. Those named will be presented with awards at a dinner to be held in New York, N. Y., February 27, 1940, and previously honored in their own communities.

A committee of 80 industrialists, headed by Robert L. Lund, executive vice-president, Lambert Pharmacal Company, and chairman, patents and trademarks committee, NAM, has been appointed to promote the search for outstanding inventors. Committee members include W. S. Gifford (A'16) president, American Telephone and Telegraph Company; and David Sarnoff (M'23) president, Radio Corporation of America. Manufacturers, trade groups, and scientific organizations have been asked to nominate persons for distinction as "Modern Pioneers."

Medals Awarded by Society of Chemical Industry

The Chemical Industry Medal, awarded annually for valuable application of chemical research to industry, has been presented this year to Doctor Robert E. Wilson, president, Pan-American Petroleum and Transport Company. The award was made November 10, 1939, at a joint meeting of the American Section of the Society of Chemical Industry, the New York Section of the American Chemical Society, and the New York Section of the American Institute of Chemical Engineers.

The Perkin Medal of the Society of Chemical Industry has been awarded for 1940 to Doctor Charles M. A. Stine, vice-president in charge of research, E. I. du Pont de Nemours and Company, Wilmington, Del. The award, given annually for valuable work in applied chemistry, is made by a committee representing the five chemical societies in the United States. It will be presented to Doctor Stine at a meeting to be held in New York, N. Y., January 12, 1940.

Electrical Insulating Materials. The 1939 edition of ASTM Standards on Electrical Insulating Materials, recently issued, contains specifications and tests on insulating varnishes and related products; molded materials; plates, tubes, and rods; insulating oils; glass; rubber products; asbestos yarns, and other products. Standarized procedures for electrical tests are included. Copies of the paper-covered 320-page publication may be obtained from the American Society for Testing Materials, 260 Broad Street, Philadelphia, Pa., at $2.00 per copy.

Texas Dam Project. Marshall Ford Dam on the Colorado River of Texas near Austin will be increased in height by 78 feet, to a total of 270 feet, in accordance with plans for the second stage of this development as approved by the last Congress. Under construction by the United States Bureau of Reclamation, the project is intended for flood control, power, and river regulation for navigation purposes. The power plant is scheduled to be built as part of the second-stage development, and is planned to accommodate three 20,000-kw units.

Lamme Graduate Scholarship Awarded. The Benjamin Garver Lamme Graduate Scholarship, awarded annually by the Westinghouse Electric and Manufacturing Company to one of the young engineers employed by the company, has been presented for the year 1939–40 to G. W. Jernstadt, chemical engineer in the meter division of the company at Newark, N. J. He will use the scholarship, which provides $1,500 for advanced study, for research on beryllium electroplating of copper.

Current Items From American Engineering Council

Committee Hears Criticism of Civil Service

Many suggestions for the improvement of the Federal civil service were presented at a public hearing held November 1 and 2 in Washington by the President's Committee on Merit System Improvement, which is now preparing a report that is expected to recommend the inclusion of higher grades of professional employees within the classified civil-service system, as well as other changes.

The committee, which is headed by Supreme Court Justice Stanley Reed, includes in its membership Gano Dunn (A'91, F'12) and General Robert E. Wood as representatives of the engineering professions and of business, respectively, as well as a number of high government officials. It was formed by President Roosevelt last February and has been studying the problem since that time. The submission of a final report is anticipated in the near future.

Of the many persons who presented testimony at the hearing only one, General Counsel D. W. Robinson, Jr., of the Federal Power Commission, opposed the further extension of the merit system. It was Mr. Robinson's contention that because of the peculiar requirements of his agency it could do a better job of selecting its legal staff than could the Civil Service Commission. All other witnesses supported the merit system in principle, but most of them submitted specific criticisms of the manner in which it is now functioning, and some recommended material modification of its procedure in recruiting employees for the more responsible positions. A representative of the Department of Agriculture, for example, suggested that its scientists and other experts be selected by joint boards made up from the Civil Service Commission, the Department itself, and one or more outside experts in the specific field involved.

Complaints directed at the administration of the present system may be briefly summarized as follows: Its examinations and lists are too general; too much time is consumed in the preparation of examinations, grading, and the compilation of eligible lists; registers are frequently too old; classification of jobs looks more to the number of subordinates controlled than to the real responsibilit es of the position; present promotion and transfer procedure is inadequate; more attention should be paid to the training of employees for promotion. (Those who attended AEC's annual assembly last January will remember that at that time a representative of the Civil Service Commission recognized the validity of similar criticisms, but contended that their remedy was largely in the provision of more personnel and funds.)

Provisions of basic civil-service law that came in for criticism included the state quota system and the preference granted to war veterans in grading applicants.

Plan Pan-American Scientific Meeting

Preliminary plans for the eighth American Scientific Congress, to be held in Washington May 10–14, 1940, have been announced by the Department of State, following the dispatch of invitations to participate to the governments of all American republics affiliated with the Pan-American Union. An organizing committee has already been named, headed by Under-Secretary of State Sumner Welles and composed of heads of a number of scientific and governmental bodies, including Doctor C. G. Abbot and Doctor Alexander Wetmore, Smithsonian Institution; Doctor Isaiah Bowman, Johns Hopkins University; Doctor Vannevar Bush (A'15, F'24) Carnegie Institution; Doctor Ross G. Harrison, National Research Council; Doctor James Brown Scott, Carnegie Endowment for International Peace; and Doctor Leo S. Rowe, Pan-American Union.

At a recent meeting of this organizing committee it was decided to divide the congress into eleven sections, each to be in charge of a chairman to be assisted by a vice-chairman and a section committee, which will soon be selected. The sections will cover, respectively, the anthropological sciences; the biological sciences; the geological sciences; agriculture and conservation; public health and medicine; physical and chemical sciences; statistics, history, and geography; international law, public law, and jurisprudence; economics and sociology; and education.

Standards

Trends in AIEE Standards Work

In recent years there have been two important trends affecting the work of the Institute on standards for electrical apparatus. These trends are discussed in the article that follows by R. E. Hellmund, chairman, AIEE standards committee, and P. L. Alger, chairman, co-ordinating committee 4.

On one hand, the broadening commercial interest in standards requires the participation and approval of a continually increasing number of persons in the development of any particular standard. To meet this, the American Standards Association has developed the system of Sectional Committees, with representation from all interested suppliers and users, and also from the public, who must give final approval to American standards. The Institute may serve as a sponsor in the preparation of an electrical standard, but usually there are other groups which have an active interest also. The continually increasing variety of electrical products and the diversity of their characteristics and applications require a corresponding increase in the number of specific standards to serve the industry.

These trends have changed the character of the Institute work on standards, but have by no means decreased its importance. The broad objective of the Institute in this field remains as before—that of giving effective service to the electrical industry, promoting clarity of understanding, adequate measurements of performance, and guidance toward the minimum variety of standards having the maximum uniformity between them consistent with progress.

Under present-day conditions, there are four specific objectives toward which the standards activities of the Institute are especially directed:

(1) There is the very important purpose of supplying essential basic facts and technical information necessary for establishing proper numerical values for standard temperatures, voltages, torques, and other characteristics that must be fixed in commercial standards. Similarly, various methods of test must be explained and discussed so that the best procedure may be decided upon for conducting guarantee tests or giving other proofs of performance. Gathering and presenting technical information to the electrical industry through the medium of ELECTRICAL ENGINEERING and the various Institute conventions is, of course, the prime purpose of the Institute's existence. While the connection between many Institute papers and standards appears remote, it is nevertheless true that nearly all technical papers recorded in the TRANSACTIONS have in some degree an influence on the standards adopted by the industry. Examples of recent papers of particular importance in this field are those given at the Symposium on Rating of Electrical Apparatus conducted at the 1939 winter convention, the supplementary paper on Ambient Temperature presented at the combined summer and Pacific Coast convention at San Francisco, and the very recent papers presented at the standards session of the Middle Eastern District meeting at Scranton, Pa. Other activities of the Institute relating to standards are the development of the several Institute test codes, and the series of technical conferences held at recent conventions to discuss such codes.

(2) The Institute performs an important service in supplying definitions of electrical terms and other educational material and in promoting understanding of tests and other procedures involved in the application of standards in particular cases. Even though a standard may be clearly written and its application in normal cases may be noncontroversial, there frequently arise border-line cases in which the underlying thought or objectives held in view by those who compiled the standard should be understood for its proper interpretation. By committee reports and explanatory articles frequently prefacing a standard or recorded during its compilation, the Institute provides a helpful clarification of ideas.

(3) Another function of the Institute, which seems of increasing importance under present conditions, is that of providing guidance in the development of new standards. The multiplicity of standards now required gives rise to many cases of overlapping or conflicting rules as well as to unnecessary multiplicity of numerical values and other limits which must be complied with. By the development of general principles and of preferred numerical values that can be followed by individuals in widely different fields who compile standards for specific types of apparatus, the co-ordination of types of apparatus to be associated is greatly facilitated. For example, the associated use of transformers, lightning arresters, circuit breakers, bushings, and insulators on high-voltage systems requires that reasonably consistent insulation levels be adhered to in all components of the system, and this in turn requires that the impulse test values and other insulation requirements be co-ordinated in the various specific standards.

Likewise, without such general guiding principles, a great many more numerical values of standard voltages, standard temperature limits, and other characteristics would be specified than are at all necessary for adequate service to the industry. An example of the Institute's work in this field is the revision of AIEE Standard No. 1 now being carried on under the auspices of the standards committee. Similarly, work is being done on the simplification and co-ordination of insulation tests, the lining-up of standard reference temperatures, and other matters, with a view to promoting the maximum consistency between American and international standards, as well as among the Institute standards themselves.

Under present world conditions, it is especially important that further study be given to correlation of the IEC and American standards in view of the increasing foreign demand for American apparatus and the necessity for co-ordinating it with existing systems built up on the basis of IEC rules.

(4) Besides these services in the way of supplying facts, definitions, guiding principles, and educational material, all of which must precede and underlie the development of sound standards, the Institute acts as sole or joint sponsor, under the ASA rules, for the development of many specific standards in the electrical field. Throughout the electrical field, the Institute has every right, and an obligation, to propose new or improved standards, and the technical sessions of the Institute should serve as a proving ground for testing the worth of all new ideas along these lines. Of course all such standards developed or proposed by the Institute must go through the ASA Sectional Committee procedure and win the approval of the other interested groups before final adoption, and these other groups will introduce additional features in the standards, particularly with reference to standard sizes, speeds, characteristics, and other subjects of primary commercial importance. However, the Institute's part in providing fundamental knowledge and technically sound procedures will always remain a vital element in standards development.

Commercial standards developed through the ASA procedure are often a long time being completed and usually remain in force for extended periods. Furthermore, changes in such standards usually should consist in modifications rather than revolutionary alterations, as otherwise the stability necessary for commercial progress cannot be obtained. On the other hand, the Institute publications on its standards, especially those dealing with guiding principles, improved test methods, and other matters which should be considered whenever changes in the more commercial standards are timely, should flow in a continuous stream, with frequent revisions and additions, so as to be up to date at all times. By thus keeping available in convenient reference form a complete set of facts and principles for the guidance of those responsible for the preparation of commercial standards, the Institute can render a most important service to the industry.

United Engineering Trustees, Inc.

Joint Organizations of the Engineering Societies

United Engineering Trustees, Inc., one of the joint agencies supported and participated in by the AIEE, was organized in 1904 as an instrumentality of the four national societies of civil, mining and metallurgical, mechanical, and electrical engineers. Its purpose is the management of property and funds in which these societies have joint interests, and it is governed by trustees duly appointed by the societies as their representatives. It maintains two departments: the Engineering Societies Library, and The Engineering Foundation.

The corporation (UET, Inc.) manages the Engineering Societies Building and all trust funds placed in the hands of UET, Inc.

The Engineering Societies Library is a free public engineering library, which is operated for users at a distance, as well as for those who visit its rooms in the Engineering Societies Building.

The Engineering Foundation, founded by the late Ambrose Swasey in 1914, is entrusted with the expenditure of income from endowment and other funds. The ultimate objective of the Foundation is stated to be: "the furtherance of research in science and engineering, or for the advancement in any other manner of the profession of engineering and the good of mankind."

In the accompanying articles may be found announcements of the elections recently held by UET and The Engineering Foundation, and abstracts of the annual reports of these organizations and of the Engineering Societies Library.

Election of Officers of United Engineering Trustees, Inc.

Officers to serve the United Engineering Trustees, Inc., for the year 1939–40 were elected at the recent annual meeting of UET. H. A. Lardner (A'94, F'13) was elected president. J. P. H. Perry was elected vice-

president, and Albert Roberts re-elected vice-president. H. R. Woodrow (A'12, F'23) was re-elected treasurer; W. D. B. Motter, Jr., was elected assistant treasurer; and John H. R. Arms was re-elected secretary and general manager. The first five constitute the executive committee.

Members of the board of trustees of UET for the year 1939–40, including both new and holdover members, with the societies they represent, are:

Terms expiring October 1940

Otis E. Hovey, ASCE
W. D. B. Motter, Jr., AIME
Kenneth H. Condit, ASME
H. R. Woodrow (A'12, F'23), AIEE

Terms expiring October 1941

A. L. Queneau, AIME
F. M. Farmer (A'02, F'13, president), AIEE

Terms expiring October 1942

J. P. H. Perry, ASCE
H. A. Lardner, ASME

Terms expiring October 1943

J. P. Hogan (M'31), ASCE
Albert Roberts, AIME
D. Robert Yarnall, ASME
C. E. Stephens (M'22), AIEE

Of these C. E. Stephens is newly appointed; J. P. Hogan, Albert Roberts, and D. Robert Yarnall are reappointed; all others were held over.

Newly appointed to the finance committee are Otis E. Hovey and C. E. Stephens; reappointed, Albert Roberts, *chairman*, and H. R. Woodrow. C. E. Stephens was appointed to the real estate committee; J. P. H. Perry, chairman, A. L. Queneau, and Kenneth H. Condit were reappointed. H. A. Lardner is a member ex officio of both committees.

Engineering Societies Library Officers and Board Elected

At the recent annual meeting of United Engineering Trustees the following officers were appointed or reappointed to the board of the Engineering Societies Library: A. R. Mumford, chairman; J. W. Laist, vice-chairman; Harrison W. Craver, director. Mr. Craver is also secretary, ex-officio.

Members of the Library board appointed or reappointed for 1939–40, with the societies they represent, are:

Terms expiring October 1940

C. E. Trout, ASCE
J. W. Laist, AIME
A. R. Mumford, ASME
W. A. Del Mar (A'06, F'20), AIEE
A. W. Berresford (A'94, F'14, past-president), member-at-large

Terms expiring October 1941

J. K. Finch, ASCE
F. F. Sharpless, AIME
John Blizard, ASME
W. I. Slichter (A'00, F'12), AIEE
S. H. Ball, member-at-large

Terms expiring October 1942

J. J. Yates, ASCE
Thomas T. Read, AIME
W. E. Spear, member-at-large

Terms expiring October 1943

E. E. Church, Jr., ASME
W. S. Barstow (A'94, F'12), AIEE
W. D. B. Motter, Jr., UET board of trustees

Ex officio

G. T. Seabury, secretary, ASCE
A. B. Parsons, secretary, AIME
C. E. Davies, secretary, ASME
H. H. Henline, national secretary, AIEE
Harrison W. Craver, director, Engineering Societies Library

Members of the executive committee for 1939–40 are:

W. S. Barstow
J. K. Finch
W. D. B. Motter, Jr.
W. I. Slichter

Engineering Foundation Elects Officers

At the recent annual meeting of The Engineering Foundation, G. E. Beggs was elected chairman for 1939–40, and O. E. Buckley (M'19, F'29) was elected vice-chairman. These two, with F. F. Colcord, Kenneth H. Condit, and A. L. Queneau, comprise the executive committee. Other re-elections were O. E. Hovey, director, and John H. R. Arms, secretary.

The officers and executive committee are elected by The Engineering Foundation board from among its own members. The board is itself elected by the board of trustees of UET, Inc. Complete list of its members for 1939–40, with the societies each represents and the time at which each term expires, is as follows:

Four Trustees of UET, Inc.		
A. L. Queneau	AIME	1941
Kenneth H. Condit	ASME	1943
O. E. Hovey	ASCE	1941
H. R. Woodrow	AIEE	1943
Eight Members Nominated by Founder Societies		
G. D. Barron	AIME	1940
G. E. Beggs	ASCE	1943
F. F. Colcord.	AIME	1942
F. M. Farmer	AIEE	1943
W. H. Fulweiler	ASME	1940
A. A. Potter	ASME	1943
E. M. T. Ryder	ASCE	1942
W. I. Slichter	AIEE	1940
Three Members-at-Large		
O. E. Buckley	AIEE	1942
J. V. N. Dorr	AIME	1943
E. R. Fish	ASME	1943
President of UET, Inc., ex officio		
H. A. Lardner	ASME	

Members of the research procedure committee, as appointed or reappointed, with the societies they represent, are:

O. E. Buckley, The Engineering Foundation.

K. H. Condit, The Engineering Foundation.

L. W. Chubb (A'09, F'21, past director) director, research laboratories, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., AIEE.

W. H. Fulweiler, Philadelphia, Pa., ASME.

Sam Tour, Lucius Pitkin, Inc., New York, N. Y., AIME.

H. E. Wessman, New York University, New York, N. Y., ASCE.

G. E. Beggs, ex officio.

The personnel of other committees as appointed or reappointed at this meeting is as follows:

Iron Alloys

G. B. Waterhouse, *chairman*, professor of metallurgy, Massachusetts Institute of Technology, Cambridge, Mass., representing AIME.

Lyman J. Briggs, director, National Bureau of Standards, represented by J. G. Thompson, chief of section on chemical metallurgy, National Bureau of Standards, Washington, D. C.

J. W. Finch, director, United States Bureau of Mines, represented by R. S. Dean, chief engineer, Metallurgical Division, Washington, D. C.

J. T. MacKenzie, metallurgist and chief chemist, American Cast Iron Pipe Company, Birmingham, Ala., representing American Foundrymen's Association.

John Johnston, director of research, United States Steel Corporation, Kearny, N. J., representing American Iron and Steel Institute.

Bradley Stoughton, dean of engineering, Lehigh University, Bethlehem, Pa., representing American Society for Metals.

Jerome Strauss, vice-president, Vanadium Corporation of America, Bridgeville, Pa., representing American Society for Testing Materials.

T. H. Wickenden, metallurgical engineer, International Nickel Company, New York, N. Y., representing The Society of Automotive Engineers.

J. H. Critchett, vice-president, Union Carbide and Carbon Research Laboratories, Inc., New York, N. Y., representing American Electrochemical Society.

Wilfred Sykes (A'09, F'14, past manager) assistant to the president, Inland Steel Company, Chicago, Ill.; member-at-large.

F. T. Sisco, *editor*.

Welding Research

C. A. Adams (A'94, F'13, past-president) *chairman;* consulting engineer, Edward G. Budd Manufacturing Company, Philadelphia, Pa.

H. C. Boardman, research engineer, Chicago Bridge and Iron Company, Chicago, Ill.

Everett Chapman, president, Lukenweld, Inc. Coatesville, Pa.

J. H. Critchett, vice-president, Union Carbide and Carbon Research Laboratories, Inc., New York N. Y.

J. J. Crowe, engineer-in-charge of apparatus, research, and development department of Air Reduction Company, Jersey City, N. J.

A. S. Douglass, construction engineer, Detroit Edison Company, Detroit, Mich.

C. L. Eksergian, chief engineer, Budd Wheel Company, Detroit, Mich.

A. J. Ely, mechanical engineer, Standard Oil Development Company, Elizabeth, N. J.

H. M. Hobart (A'94, F'12, past vice-president) consulting engineer, General Electric Company, Schenectady, N. Y.

D. S. Jacobus (A'03) advisory engineer, The Babcock and Wilcox Company, New York, N. Y.

G. F. Jenks, colonel, Ordnance Department, United States Army, Washington, D. C.

F. H. Frankland, chief engineer, American Institute of Steel Construction, New York, N. Y.

P. G. Lang, Jr., engineer of bridges, Baltimore and Ohio Railroad, Baltimore, Md.

F. T. Llewellyn, research engineer, United States Steel Corporation, New York, N. Y.

R. E. Zimmerman, vice-president, United States Steel Corporation of Delaware, Pittsburgh, Pa.

William Spraragen (A'17, M'26) *secretary*, technical secretary and editor, New York, N. Y.

Chairman Beggs was appointed Foundation's representative on the executive board of National Research Council, and O. E. Hovey reappointed representative on the highway research board.

Annual Report Issued by United Engineering Trustees, Inc.

The annual report of United Engineering Trustees, Inc., for the year ending September 30, 1939, has been submitted to the AIEE and other participating societies by D. Robert Yarnall, president.

During the year considerable study has been given to clarifying and simplifying the work of the organization, and the "History, Charter, and Bylaws" has been revised and brought up to date, as a guide to the

trustees and for the information of the Founder Societies or others wishing to contribute to the work.

Maintenance of the Engineering Societies building, now 33 years old, may be expected to necessitate increasing renewals. New heating system controls have been installed, improvements in ventilating the meeting halls made, and other routine maintenance carried on. The property remains tax exempt, as does the corporation. Use of meeting rooms has been provided gratuitously to federal, state, and municipal organizations for military and WPA projects and to the WPA and the New York City Board of Education for adult education classes.

Both the John Fritz Medal Fund Corporation and the Daniel Guggenheim Medal Fund, Inc., dissolved their corporations in 1939 and became committees of UET, Inc.

Operation of Engineering Societies Building

Operating revenue	$157,381.07
Less operating expenditures	158,117.77
Operating deficit	736.70
Operating credit from previous years	13,038.14
Net credit balance September 30, 1939	$ 12,301.44

Formal acceptance of responsibility for these dissolved corporations represents a change in name rather than in procedure, since the funds of the former and the books of the latter had been managed by UET, Inc., for a long period. UET, Inc., is treasurer of the Engineers Council for Professional Development, and custodian of the Engineering Societies Employment Service relief fund.

The corporation's investment advisers report that, as compared with a year ago, the total current value of its portfolio has increased slightly, and while the current return is slightly reduced, the corporation's position is considerably more liquid. Summaries of the UET finance committee's report on funds and property and on operation of the Engineering Societies building are contained in the accompanying tabulations.

Summary of UET, Inc., Finance Committee Report on Funds and Property

		Book Value	Market Value
Combined Fund:* Summary of investments September 30, 1939			
Funds included			
Engineering Foundation fund		$ 895,849.50	
Edward Dean Adams fund		90,911.06	
Library endowment fund		168,346.55	
Depreciation and renewal fund		415,225.16	
General reserve fund		10,193.86	
Total		$1,580,526.13	
Investments			
Legal	$ 393,200.75		
Nonlegal	1,097,382.30		
Total investments September 30, 1939	1,490,583.05		
Cash uninvested	89,943.08		
Total	$1,580,526.13		$1,317,399.52
Other Funds and Property			
Real estate, cost of, September 30, 1939		$1,993,793.92	
Henry R. Towne engineering fund investments		46,040.63	25,977.21
Henry R. Towne engineering fund uninvested cash		2,592.90	
The D. Guggenheim Medal board of award investments, net		15,840.00	8,262.10
The John Fritz Medal board of award investments		3,500.00	3,482.50
UET, Inc., operating assets		10,986.05	
UET, Inc., accounts receivable, gross		1,315.39	
Engineering Societies Library maintenance assets		12,051.25	
Library Service Bureau accounts receivable, gross		980.96	
Gift for endowment committee cash		653.62	
The Engineering Foundation—unexpended income		23,884.80	
Alloys of iron research—unexpended income		7,088.30	
Welding research—unexpended income		726.05	
The Engineering Foundation custodian fund cash		6,623.44	
United Engineering Trustees, Inc., custodian funds—unexpended income		1,547.40	
Total		$3,708,150.84	

* A group of funds managed as one for convenience and economy in investment transactions.

Annual Report Issued by Engineering Societies Library

The annual report of the Engineering Societies Library for the year ending September 30, 1939, has been submitted to the AIEE and other participating societies by Harrison W. Craver, director.

The year was again marked by increased use and enlargement of facilities. The library had more readers than during the preceding year, made more searches, translations, and photostatic prints, and answered more requests for information. The library was used by 43,110 persons, 1,160 more than in the preceding year. Of these, 32,471 visited it in person. The remaining 10,639 were nonvisitors who were assisted in various ways. Translations were made for 113; for 2,508 of them 21,907 photoprints were made, 91 presented inquiries that necessitated extensive searches. Books to the number of 186 were lent to 162 members, information was provided to 2,985 persons by mail and 4,780 by telephone.

During the year, the collection was increased from 144,262 volumes, 7,408 maps, and 4,391 searches, to 146,999 volumes, 7,564 maps, and 4,440 searches. In addition 3,399 pamphlets were added to volumes already counted. There are also approximately 13,000 duplicate volumes on hand. Cataloging is up to date. In addition, further progress has been made in recataloging the pamphlets in the Wheeler collection of electrical books.

The work of repairing and rebinding valuable rare books, which was so generously supported by W. S. Barstow (A'94, F'12), was completed in March. In all, 1,268 volumes were repaired during the three years that this work was in progress.

Operation of Engineering Societies Library

Maintenance revenue	$45,947.94	
Maintenance expenditures	46,836.38	
Debit balance for year 1938–39	888.44	
Credit balance from previous years	6,072.23	
Credit balance September 30, 1939		$ 5,183.79
Service bureau revenue	8,512.24	
Service bureau expenditures	7,169.41	
Credit balance for year 1938–39	1,342.83	
Credit balance from previous years	6,223.56	
Credit balance September 30, 1939		7,566.39
Total net operating credit balance cumulated to September 30, 1939		$12,750.18

The periodical index now contains over 242,000 references to important periodical articles, classified by the system used for the book collection. The number of readers who consult this index is growing steadily.

Through purchase and gift the library acquired 15,914 items; 4,340 books, 11,348 pamphlets, 177 maps, and 49 searches. Of these receipts 6,491 items were suitable additions to the library. Of the remainder, 99 books (duplicates) were added to the lending collection and 9,074 items were added to the duplicate collection. Sales of duplicates during the year amounted to $384.42.

The number of periodicals received currently was 1,340. Publishers presented 627 books, for which the library staff prepared book notices which were published in the various Society journals. If purchased, these books would have cost approximately $2,000.

During the year 14,358 gifts were received. Among the donors were Mrs. Henry Alexander, G. M. Basford, F. G. Clapp, H. A. Hopf, Murray and Flood (electrical books and periodicals), the New York Society Library, and *Engineering News-Record*.

In June, the director was asked by the American Library Association to represent it at the 1939 meeting of the International Committee of the International Federation of Library Associations, held in The Hague and Amsterdam. In connection with the trip, a number of large technical libraries in Great Britain and Scandinavia were visited.

In appreciation of the various benefactions of W. S. Barstow, a dinner was tendered him on May 15, at the Lotos Club,

with Gano Dunn (A'91, F'12) as toastmaster.

The budget for general operations was $49,100. Of this sum $35,959.70 was appropriated by the Founder Societies on a membership basis as follows:

American Society of Civil Engineers	$9,631.40
American Institute of Mining and Metallurgical Engineers	7,520.90
American Society of Mechanical Engineers	9,118.10
American Institute of Electrical Engineers	9,689.30

Expenditures from this budget amounted $46,836.38, of which $8,702.45 was spent for books and other equipment of permanent value. The service bureau received $8,512.24 in payment for translations, searches, and copies, and spent $7,169.41. The accompanying tabulation summarizes the year's operation of the library.

Annual Report Issued by Engineering Foundation

The annual report of The Engineering Foundation for the year ending September 30, 1939, has been submitted to the AIEE and other participating organizations by F. M. Farmer (A'02, F'13, president) chairman of the Foundation board, who was assisted by Otis E. Hovey, director.

The financial statement shown in the accompanying tabulation summarizes the present capital funds of the Foundation, and the incomes and expenditures during the year. Three additions to the capital funds were made during the year, as follows:

Sophie M. Gondron bequest	$25,000.00
Karl Emil Hilgard bequest	1,776.63
Richard Khuen, Jr., bequest	2,000.00
Total	$28,776.63

The Foundation sponsors and assists a wide range of research projects, most of them of a technical engineering character, but some dealing with non-technical matters of concern to engineers, educators, and the public. Work on 16 active projects, in 2 of which the AIEE is directly interested, has progressed during the report year. These projects included 33 specific problems. Fifteen formal applications for appropriations embodying over 30 specific problems were received, and grants were recommended for 14 of them. To establish personal contact and closer co-operation with those in charge of technical researches receiving Foundation support or assistance, the director has visited during the year four institutions and laboratories at which work is in progress. A summary of activities follows:

Soil Mechanics and Foundations Division (ASCE). The following studies are included:

Earths, Dams, and Embankments ($800). The committee has accumulated and studied data on methods used for recording static water pressure in embankments and foundations. Plans of the three most promising methods have been sent to committee members, with a view toward preparing a report upon the method, or methods, which appear to be the best.

Sampling and Testing ($5,000). The principal work of this committee continues to be the development of better methods and equipment for obtaining undisturbed samples. A report was presented to ASCE in April 1939, and will be prepared in final form at an early date.

Summary of Engineering Foundation Financial Resources

Capital Funds		
Endowment, total book value September 30, 1939		$990,000.00
E. H. McHenry bequest, in hands of executors during life of two beneficiaries, appraised at probate of will in 1931, approximately		$400,000.00

The capital funds are held and administered by United Engineering Trustees, Inc. The net income from endowment was $38,838.37 for the fiscal year ended September 30, 1939. The Foundation board has discretion in use of income. For many of the enterprises which the Foundation has aided, large contributions of money, services, and materials have been obtained from others. Money "contributions" from organizations and individuals, for specific activities, passed through the Foundation's accounts from its organization to September 30, 1939, totaled $355,557.56.

Expendable Resources—Summary		
Balance October 1, 1938		$29,873.42
Receipts		
Income from endowment	$38,838.37	
Income from minor items	181.11	39,019.48
Total Resources		$68,892.90
Disbursements—Summary		
Research projects	$33,865.79	
Promotion of research and administrative expenses	11,142.31	
Total for furtherance and support of research		$45,008.10
Balance October 1, 1939		$23,884.80

Seepage and Erosion ($800). The committee has sponsored the continuance of model studies on earth dams, and has taken under consideration the problem of rational design of drainage filters for dams, dikes, and levees. Information on filters from tests under way is being collected and will be submitted at an early date as an interim report summarizing and correlating the results from various sources. The main objectives of the study at present are development of relatively simple grading requirements for filter materials, and development of relatively simple tests to determine the suitability of local materials for filters.

Foundations ($800). The committee has collected data and is compiling results of observations on lateral earth pressures and pressures on concrete forms. Observations on the behavior of typical "quick" sands and their mechanical and hydraulic properties are being made. Settlement observations are being made on one large new structure. Thirty-three special leveling plugs were made for this purpose and installed both inside and outside of the building at the earliest possible stage of construction, and observations of settlement are being made periodically.

Project 38-d-1 ($150). Funds granted for this year could not be used on account of the unsettled conditions in Europe. No grant was requested for the coming year.

Special Committee on Hydraulic Research (ASCE). The following studies are included:

Conversion of Kinetic to Potential Energy in Expanding Conduits ($74). Tests on flow through a three- to five-inch sudden expansion have been completed. A comprehensive analysis of the photographic and statistical data obtained in experiments on this project has been made. Some warping of the pyralin sections having affected the results, it is planned to make additional experiments with "Lucite" before publishing the results of the project.

Traveling Waves on Steep Slopes ($300). The extensive motion pictures of these experiments are being studied, together with the other data obtained, with a view to issuing a comprehensive report.

Phenomena of Intersecting Streams ($300). A progress report on the results obtained with intersecting closed channels was submitted to the committee. Experiments were made on both combining and diverging flow. It is planned to continue the experiments with closed channels during the coming year.

Curves in Open Channels ($300). The nature of the flow around bends has been investigated by means of a pilot tube and midget current meter as well as by photographing the movement of sawdust, confetti (for surface velocities), rice grains (bottom velocities), and drops of oil dye (interior). A mathematical analysis of the flow has been made, giving results in general agreement with the observations.

Sedimentation at the Confluence of Rivers ($300). A number of motion pictures showing the nature of the sand movement in the experimental flumes at the intersection have been made. Experiments are now being conducted with a sand having grains fairly uniform in size.

Air Resistance to Flow of Water in Open Channels ($300). Considerable study has been given to this project and a suitable flume has been designed having steep adjustable slopes.

Simultaneous Flow of Liquids and Gases in Pipes ($300). A long closed rectangular channel of transparent "Lucite" has been constructed for these experiments. Air and water will be supplied by means of a tank at the upper end and both measured by means of a tank at the lower end. Entrainment of air by water flowing down vertical drain pipes is also being studied.

Tension Tests of Large Riveted Joints (ASCE, $500). Grant was made in March 1939 to supplement funds available in the ASCE budget to make possible the publication in full in the *Proceedings* of the society a valuable article entitled "Tension Tests of Large Riveted Joints" by Raymond E. Davis, Glenn B. Woodruff, and Harmer E. Davis.

Alloys of Iron Research (AIME, $5,000). Monograph 10, "The Alloys of Iron and Nickel, Volume I—Special Purpose Alloys" was published November 1, 1938. The manuscript of monograph 11, "The Alloys of Iron and Chromium, Volume II—High-Chromium Alloys" was delivered to the publisher in August. Work has been continued on monograph 12, "The Alloys of Iron and Nickel, Volume II—Steels and Cast Iron." Recent foreign- and English-language journals were reviewed and abstracted for monographs 11 and 12. Several hundred abstracts were added to the files.

Barodynamic Researches (AIME, $2,500). The work has been continued particularly with reference to artificial support effects of longwall mining, photoelastics as applied to mine-pillar problems, a new method of measuring the *P* plus *Q* stresses in photoelastic work, a study of block caving, a study of the side pressures due to loose materials, and a study of the time effects to predict when a mine roof is likely to fail. Principles regarding artificial support in mines have been developed and artificial supports embodying these principles have been built and tested.

Effect of Temperature on the Properties of Metals (ASME, $1,000). A preprint of a report of joint research committee was submitted to the June 1939 meeting of the American Society for Testing Materials. It gave brief information concerning 11 research projects and appendices contained 3 technical papers. The work is being continued.

Critical Pressure Steam Boilers (ASME, $1,000). The studies on the viscosity of steam have been completed and a paper will be presented at the annual meeting of

ASME in December 1939. The work on the corrosion of steels at high temperature is in progress and a paper probably will be submitted during the next fiscal year.

Fluid Meters (ASME, $1,000). For several years the committee has had in progress a research investigation to determine the coefficients for flow nozzles. This has required much test work and the raising of a large fund to carry it on. The major task now is to correlate the data and arrange it for presentation. Articles on this research in the technical press have kept industry informed of its progress.

Lubrication (ASME, $1,000). The committee's work is progressing along four distinct lines: (*a*) the continuation and further development of the work on the mechanics of thick oil films; (*b*) correlation and publication of data on boundary lubrication, obtained in laboratories; (*c*) the general problem of thermodynamics of bearings; (*d*) the pressure-velocity relationships. Work on the bibliography of lubrication is progressing.

Cottonseed Processing (ASME, $500). Studies in cooker designs for continuous and automatic operation were initiated, as were studies for the purpose of improving the design of the batch type of cooker to permit economies in the manufacturing cost of the unit. Suitable automatic controls were also investigated for possible application with the batch type of cooker. To satisfy the demands of the industry, a commercial humidifier was designed, built, and placed in commercial operation for demonstration purposes. Specially designed hydraulic press plates were built to the specifications and installed during the concluding two weeks of experimental crushing operations on cottonseed only. Preliminary data indicate that the new press plate design permits a more rapid flow of oil which should permit shorter pressing cycles, thereby imparting greater pressing capacity. No perceptible improvement in total oil yield was obtained.

Rolling Steel (ASME, $800). A special tension apparatus has been designed and built, in which bars one inch in gauge length and $^3/_{16}$-inch in diameter can be pulled in tension at temperatures up to 1,200 degrees centigrade, and at high stretching velocities. Bars are heated in an induction furnace, which can be connected to an existing oscillator equipment. It is hoped that with this apparatus the yield stresses required to deform steel at temperatures from 20 to 12 degrees centigrade at a side range of velocities may be determined.

Mechanical Springs (ASME, $500). This grant has been used to assist in financing the publication of a treatise entitled "Strength of Metals with Special Reference to Spring Materials and Stress Concentration" by D. J. McAdam and R. W. Clyne.

Riveted Joints (ASME, $500 plus $399.13). These grants were made to provide for the publication of a bibliography constituting "A Critical Review of the Literature Concerning Riveted Joints." Unsuccessful attempts were made to collect enough funds to provide for 1,000 copies of a printed book. It is now proposed to print about 300 copies at the smallest practicable cost for limited distribution.

Stability of Impregnated-Paper Insulation (AIEE, $2,000 plus $267). The current year has seen the completion of a study of the influence of a variation in the density of the paper on the stability of high-voltage impregnated-paper insulation, and the completion of a similar study of the variation of the thickness of the paper of one value of density as used in practice. A number of auxiliary studies on the mechanism of breakdown in impregnated paper have been highly successful in that it became possible to interrupt the process of breakdown or failure at shorter and shorter intervals after it begins, so that much new light has been thrown on the hitherto obscure problem of the original causes of failure. These programs were planned in conferences between an advisory committee of the AIEE, a similar committee from an association of manufacturers of high-voltage cables (IPCEA), and the committee in charge of this research. The results obtained have excited great interest among the engineers of manufacturing and public-utility companies.

Welding Research Committee (AIEE and American Welding Society, $4,000 plus $5,439 reappropriated). Forty-eight reports were issued by the committee during the past year. Of these 18 related to fundamental research investigations; 11 resulted from the activities of the industrial research division; 12 were critical digests of the world's literature prepared under the auspices of the literature division; and the remainder were of a general nature. These reports covered a wide variety of subjects, including resistance welding, electrodes, stress distribution in welds subject to bending, welding arcs, spot welding low-carbon and stainless steels, crater formations in arc welding, residual stresses in pipe welding, photoelastic studies of stress distribution, miscellaneous investigations in the structural field, welds at low temperatures, control of distortion, welding of silicon bronze, weldability of medium-carbon steels, copper welding, spot, arc, and gas welding of aluminum alloys, nickel-clad steels, and effect of carbon and manganese on weldability. The literature reviews covered such subjects as molybdenum steels, copper steels, effect of oxygen, effect of aluminum on the welding of steel, internal stresses in castings, welding coated steel, effect of sulphur and phosphorus on steel, welding chromium steels, coatings and fluxes in the welding of steel, oxygen cutting of steel, effect of carbon in plain carbon steels, and the welding of copper steels.

Committee F—Fatigue Testing (*Structural*). The specifications for welding highway and railroad bridges prepared by the AWS in co-operation with the American Railway Engineering Association and the American Association of State Highway Officals assign certain values to welded joints subject to variable stresses. The fatigue values in these specifications are based upon European test results. It is desirable that a comprehensive set of data on unit design stresses of welds subjected to varying stresses be made available in this country on American materials and using American technique. Such data are also needed in the design of naval and other vessels, and in other structures subject to dynamic stresses. A comprehensive program is in progress. Some $25,000 has been contributed by government bureaus and industrial organizations, and other organizations have contributed materials and services. The first progress report was presented at the annual meeting of the AWS in October 1939.

Engineers' Council for Professional Development (General program $500, engineering schools $3,500). Work of the following committees is included:

Committee on Engineering Schools. The committee has continued to concentrate on its major objective, the accrediting of engineering curricula. During the year 4 new institutions, representing a total of 7 curricula, were inspected, and 74 curricula already provisionally accredited were reinspected. In addition, 9 curricula, rejected as a result of previous inspections, were reinspected. To date, 144 out of a total of 150-odd degree-granting engineering institutions have applied to the committee for inspection, and 687 curricula have been acted upon or are now under consideration. With the work of initial inspection and recommendation so nearly complete, the committee has directed its attention toward insuring the continued reliability of its list of accredited curricula. It also has devised and proposed a method of financing the continuing program of reinspections. Out-of-pocket expenses incurred in visiting institutions will be covered by an annual accrediting fee paid by institutions having curricula accredited, the fee paid by any one institution to be based on engineering enrollment and the number of curricula accredited. More than half the institutions involved already have approved the plan and only two have disapproved. In 1938 the committee secured funds from the Carnegie Foundation for the Advancement of Teaching to defray the cost of analyzing the vast amount of data collected during the course of the accrediting program. The report of this study has been completed and is now being printed.

Selection and Guidance of Engineering Students. The committee has continued to encourage the formation of guidance committees of engineers to co-operate with high-school principals in counseling with boys who are thinking of an engineering career. Numerous cities have well-functioning committees. About 7,000 copies of "Engineering: a Career—a Culture" have been distributed since October 1938, and a revision is now in progress.

Professional Recognition. The committee has reviewed the different agencies by which certificates are awarded, which are more or less generally accepted as indicating "professional recognition" and finds that these vary widely. There are: (*a*) the engineering societies, both the leading professional societies and prominent state or local societies. The national societies have different grades of membership which differ in title and in the requirements for admission; and they deal with particular fields of engineering; (*b*) the engineering schools, which grant degrees for graduate study; their character differs widely—some are broad and some are specialized, many are of recent development; no accrediting has been made and they vary in requirements as to curricula and experience; (*c*) the state registration boards which grant certificates for the legal practice of professional engineering. These are relatively new; about 90 per cent of them have been created within 20 years, and a third of them within the past 4 years. There are now 42 states having registration laws, in general similar but a number below the average standard. These boards are, in general, appointed by the state governors. Experience is an essential requirement, this to be "satisfactory to the board." Examinations are required by most of the boards. There is a National Council of State Boards of Engineering Examiners, a voluntary organization which aims to secure more effective and uniform administration of the state laws. Consistent practices by the several states in maintaining a common standard of competency is dependent upon the voluntary action of these states.

Plastic Flow of Concrete (University of California, $2,000). Work has been continued on a study of thermal stresses on slabs. The observations on the slab during the period when its temperature rose and fell, due to heat of hydration of the cement and to natural dissipation of heat, have been completed. Work is in progress on an investigation of the plastic flow of concrete in shear. A series of tests of plastic flow during the early hardening period is being made. Tests on the plastic flow of large cylinders of job concrete of several ages are under way.

Letters to the Editor

CONTRIBUTIONS to these columns are invited from Institute members and subscribers. They should be concise and may deal with technical papers, articles published in previous issues, or other subjects of some general interest and professional importance. ELECTRICAL ENGINEERING will endeavor to publish as many letters as possible, but of necessity reserves the right to publish them in whole or in part, or reject them entirely.

ALL letters submitted for consideration should be the original typewritten copy, double spaced. Any illustrations submitted should be in duplicate, one copy to be an inked drawing but without lettering, and the other to be lettered. Captions should be furnished for all illustrations.

STATEMENTS in these letters are expressly understood to be made by the writers; publication here in no wise constitutes endorsement or recognition by the American Institute of Electrical Engineers.

Sequence Components and the Wye-Delta Transformation

To the Editor:

The problem presented by unbalanced loads or faults supplied through a wye-delta transformation has been treated by various authors (see bibliography) but the following presentation appears to the writer to possess a certain directness and generality of attack that may be of assistance to students and others.

Figure 1*a* shows a diagram of the bank connections (three single-phase transformers) with *polarity* and *terminal designation* as shown. These data, in conjunction with the *bank ratio of transformation*, are essential to the calculation of the "complex voltage and current transformation factors" that must be applied in the solution of the problem. It is assumed that the bank is supplied with a known unbalanced voltage at the supply end of the primary feeder. Transverse constants are neglected, and symmetry is assumed between source and load.

It is well known that the wye-delta transformation shifts the positive-sequence voltage and current in one direction, and the negative-sequence values an equal amount in the opposite direction. Thus, in the figure, the shift in positive-sequence voltage (as we proceed from secondary to primary circuit) is *backward* 30 degrees, while the negative sequence is shifted *forward* the same amount. In general the following complex transformation factors will apply:

$$V_{a1}=nV''_{a1}\overline{\diagdown\phi} \qquad V_{a2}=nV''_{a2}\underline{/\phi}$$

$$I_{a1}=\frac{I'_{a1}}{n}\overline{\diagdown\phi} \qquad I_{a2}=\frac{I'_{a2}}{n}\underline{/\phi}$$

where n is the "bank transformation ratio" (not winding-winding), the voltages are true line-to-neutral values, and the currents are true line values (not winding values necessarily).

Figure 1*b* shows the sequence networks for the case considered, recognizing in the separation of the secondary and primary circuits the divergent relationships which will apply to the sequence components in the two circuits; however, the circuit relations are tied together rigorously by the complex transformation factors previously referred to. No zero-sequence voltages or currents are present in the case considered, so that the corresponding network is omitted.

The following equations may then be written, wherein the primed and double-primed voltages, currents, and ohms represent true secondary values, and the unprimed quantities are true primary values. The small z's are the positive-, negative-, and zero-sequence *component* impedances derived from the unbalanced impedances making up the load. Vector voltages and currents are indicated throughout; the impedances are complex numerics.

From standard relationships:

$$V'_{a1}=I'_{a1}z'_0+I'_{a2}z'_2 \tag{1}$$

$$V'_{a2}=I'_{a1}z'_1+I'_{a2}z'_0 \tag{2}$$

Also:

$$V''_{a1}=I'_{a1}Z'_{L1}+V'_{a1}=I'_{a1}Z'_{L1}+I'_{a1}z'_0+I'_{a2}z'_2=I'_{a1}(Z'_{L1}+z'_0)+I'_{a2}z'_2 \tag{3}$$

$$V''_{a2}=I'_{a2}Z'_{L2}+V'_{a2}=I'_{a2}Z'_{L2}+I'_{a1}z'_1+I'_{a2}z'_0=I'_{a1}z'_1+I'_{a2}(Z'_{L2}+z'_0) \tag{4}$$

Equations 1 to 4 inclusive are in true secondary volts, amperes, and ohms.

Again, from fundamental theory:

$$E_{a1}=I_{a1}Z_1+V_{a1} \tag{5}$$

$$E_{a2}=I_{a2}Z_2+V_{a2} \tag{6}$$

Equations 5 and 6 are in true primary volts, amperes, and ohms. Now

$$I_{a1}=\frac{I'_{a1}}{n}\overline{\diagdown\phi} \tag{6a}$$

and

$$V_{a1}=nV''_{a1}\overline{\diagdown\phi} \tag{6b}$$

Substituting equations 6*a*, 6*b*, and 3 into 5 we have:

$$E_{a1}=\frac{I'_{a1}}{n}\overline{\diagdown\phi}\,Z_1+nV''_{a1}\overline{\diagdown\phi}=\frac{I'_{a1}}{n}\overline{\diagdown\phi}Z'_1n^2+nI'_{a1}(Z'_{L1}+z'_0)\overline{\diagdown\phi}+nI'_{a2}z'_2\overline{\diagdown\phi}$$

where Z'_1 is Z_1 in secondary equivalent value (ohms). Thus:

$$E_{a1}\underline{/\phi}/n=I'_{a1}(Z'_1+Z'_{L1}+z'_0)+I'_{a2}z'_2 \tag{7}$$

Also:

$$I_{a2}=\frac{I'_{a2}}{n}\underline{/\phi} \tag{7a}$$

and

$$V_{a2}=nV''_{a2}\underline{/\phi} \tag{7b}$$

Substituting equations 7*a*, 7*b*, and 4 into 6 we have:

$$E_{a2}=\frac{I'_{a2}}{n}\underline{/\phi}Z_2+nV''_{a2}\underline{/\phi}=\frac{I'_{a2}}{n}\underline{/\phi}Z'_2n^2+nI'_{a2}(Z'_{L2}+z'_0)\underline{/\phi}+nI'_{a1}z'_1\underline{/\phi}$$

where Z'_2 is Z_2 in secondary equivalent value (ohms). Thus:

$$E_{a2}\overline{\diagdown\phi}/n=I'_{a1}z'_1+I'_{a2}(Z'_2+Z'_{L2}+z'_0) \tag{8}$$

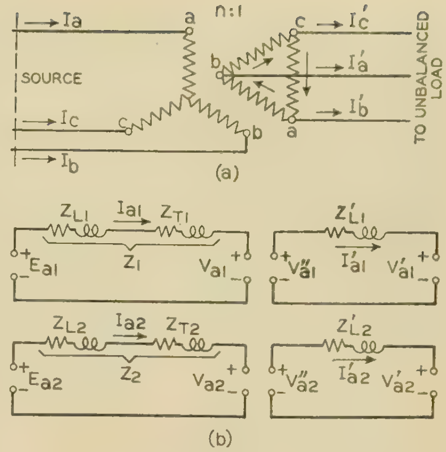


Figure 1

The left-hand members of equations 7 and 8 may be defined as the voltages impressed on the circuit in secondary equivalent values. Evidently, these equations may be solved for I'_{a1} and I'_{a2}. With these current components known, complete calculations may be readily carried out, using the various relations indicated in conjunction with well-known sequence-component synthesis. Faults, balanced load, or balanced voltage impressed at the source are merely special conditions of the general case, which is also readily extended to the case of two or more banks of transformers operated in tandem. Analogous relations apply to the delta-wye.

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Very truly yours,

WALTER A. CURRY (A'17, F'37)

(Assistant professor of electrical engineering, Columbia University, New York, N. Y.)

Why So Few Famous Engineers Today?

To the Editor:

One hundred and sixty years ago it was written:

> "It is a great and necessary proof of wisdom and sagacity to know what questions may be reasonably asked. For if a question is absurd in itself and calls for an answer where there is no answer, it does not only throw disgrace on the questioner, but often tempts an uncautious listener into absurd answers, thus presenting, as the ancients said, the spectacle of one person milking a he-goat, and of another holding the sieve." (Kant, *Critique of Pure Reason.*)

The question at the head of this letter, which serves as the text for the editorial in the October issue of ELECTRICAL ENGINEERING, seems to come within the scope of the quotation on two counts:

1. The underlying assumption cannot be admitted without proof, and no method of proof appears.

Any one of your readers can readily name several engineers, still active, whose accomplishments will measure up to those of

any of the "famous" engineers in your list or in any list that can be compiled. It would be invidious to mention names, since for every one named I should probably omit two or three equally worthy of note, but I could present a formidable list of living engineers whose eminence no one would question.

Furthermore, the number of eminent living engineers cannot fairly be compared with a roster which goes back to Leonardo da Vinci, but only with the number living at any one time. If this be done I think it will be found that the implication of the question is false.

Many years ago there was a conundrum popular among school children: "Why does a dog always sneeze three times?" Much ingenuity was exercised in attempts to explain this peculiar phenomenon. The correct answer was: "He doesn't."

2. What do you mean by a "famous" engineer? Is it the same as "notorious?" Do you mean one whose name appears frequently in the daily press and is bandied about among those who have no conception of engineering? Or do you mean one whose work has constituted a substantial advance in engineering art and science, although, in all probability, the crowd never heard of him? If the former, the fewer "famous" engineers, the better.

Showmanship (the word is a shock to the sensibilities of a professional man, but it appears twice in your editorial) may contribute to financial success, but no serious-minded engineer was ever consciously a showman.

The late Thomas B. ("Czar") Reed was once asked his definition of a statesman. He replied: "A statesman is a politician who is dead." Like most epigrams, this is only a half truth. Nearly all the "famous" engineers in your list are dead; some of them are not otherwise distinguished.

Most engineers spend their lives doing routine and conventional things in accordance with "good practice." Only at long intervals appear a few men who are intellectually far above the general level, who do not feel bound to copy the work of their predecessors, and who do not think that good practice is determined, like the choice of a Congressman, by the vote of mediocrity. Such men are rarely heard of by the public. They are eminent but not "famous."

One man builds a mile of road and attracts no attention. Another builds a thousand miles and is touted as a great engineer, although the only difference between them is that the second man has spent a thousand times as much money as the first. Such is fame!

Few great men have been recognized as such by their contemporaries. It is only after they have ceased to run that the crowd catches up with them. Schopenhauer did most of his work as a young man but received no recognition until shortly before his death. "Time has brought its roses at last," he said, "but see," touching his silvered hair, "they are white."

One must choose whether he will have the high opinion of his own kind, which comes only if it is merited, or the manufactured and meaningless press publicity which caters to a low form of personal vanity.

The whole matter has been tersely put in one of Mr. Arthur Guiterman's verses:

"Some men are famed for genius, knowledge, power,
And service to humanity; and some
Are talked about, like Pisa's leaning tower,
Because they're out of plumb."

Which do you want? Enduring fame after you are gone, or vulgar notoriety while you live? You may never have either but you can't have both.

Only a decadent civilization rates a man's achievements by the roar of the crowd.

Very truly yours,

Charles W. Comstock (A'03, M'38)

(Consulting engineer, Jackson Heights, N. Y.)

Magnesium-Copper Sulphide Rectifier Battery Charger

To the Editor:

In the September 1939 issue of Electrical Engineering I noted with interest F. J. Bartholemew's comments on C. A. Kotterman's paper dealing with magnesium-copper sulphide rectifiers. It seems to me that the magnesium-copper sulphide rectifier, due to its low efficiency and short life, is the one that should *not* be considered seriously as a heavy-duty industrial type of rectifier. Both the selenium and copper oxide rectifiers appear to show over-all efficiencies almost twice as high as that of the copper sulphide rectifier. Copper oxide rectifiers have proved their long life and there are thousands of these units in operation all over the world. Some of these installations have exceeded 12 years of continuous operation.

The selenium-iron rectifier is relatively new and has been used mostly in Germany, possibly due to the scarcity of copper. Life tests have not progressed as far as those on the copper oxide rectifier. Up to 1936 the selenium cells had a relatively short life, and it is only since 1936 that some improvements have been made, and all indications point toward a longer life. However, it can be stated with a fair degree of accuracy that up to the present time there is no dry rectifier on the market that has proved to have a long life comparable to the copper oxide rectifier, since tests on the selenium rectifiers which show merit have only been running for three years.

Very truly yours,

E. A. Harty (A'22, M'36)

(Street lighting engineering department, General Electric Company, West Lynn, Mass.)

Personal Items

F. R. Maxwell, Jr. (M'30) professor of electrical engineering, University of Alabama, Tuscaloosa, has been elected AIEE vice-president for District 4, by action of the board of directors following the resignation of E. E. George. He was born in Tuscaloosa, June 15, 1889, and received the degrees of bachelor of science in mechanical engineering (1911), mechanical engineer (1912), and electrical engineer (1923) from the University of Alabama. In 1912 he was employed by the Tuscaloosa Ice and Light Company, working on various aspects of plant operation. When the company became the Tuscaloosa Railway and Utilities Company, in 1915, he was made commercial manager, and after two years (1917–19) in the United States Naval Reserve, he returned in 1919 to be assistant general manager. He became instructor in electrical engineering and physics at the University of Alabama in 1920, becoming assistant professor the following year, associate professor in 1923, and full professor in 1934. He is counselor of the AIEE Student Branch at the University of Alabama, a member of the AIEE membership committee, and was vice-chairman of District 4. He has also been chairman of the Alabama Section. He is a member of the Society for the Promotion of Engineering Education and of Tau Beta Pi.

F. R. MAXWELL, JR.

H. A. Lardner (A'94, F'13) vice-president, J. G. White Engineering Corporation, New York, N. Y., has been elected president of United Engineering Trustees, Inc., joint agency of the national engineering societies. He was born October 1, 1871, at Oconomowoc, Wis., and received the degree of bachelor of science in electrical engineering in 1893 and that of electrical engineer in 1895 from the University of Wisconsin. He was engaged on railway construction work for J. G. White and Company, at Baltimore, Md., in 1894–95. During the next two years he was an instructor in electrical engineering at Pennsylvania State College, State College, Pa., returning to J. G. White and Company in 1897 to work in Buffalo and New York, N. Y. He became assistant electrical engineer at the New York office in 1900, then chief electrical engineer, and general manager of the engineering department. In 1909 he was made general manager of the San Francisco office of the company, where he remained until 1915. He was made a vice-president and director of

H. A. LARDNER

J. P. HOGAN

W. F. DAVIDSON

O. E. BUCKLEY

L. M. GOLDSMITH

the J. G. White Engineering Corporation in 1913. Since 1915 he has been in New York. He is a member of The American Society of Mechanical Engineers, and past-president of the New York Electrical Society, and has been a member of the board and vice-president of UET, Inc.

O. E. Buckley (M'19, F'29) executive vice-president, Bell Telephone Laboratories, Inc., New York, N. Y., has been elected vice-president of the Engineering Foundation, joint research agency of the national engineering societies. Born at Sloan, Iowa, August 8, 1887, Doctor Buckley received the degree of bachelor of science from Grinnell College, 1909, and that of doctor of philosophy from Cornell University, 1914, and the honorary degree of doctor of philosophy from Grinnell in 1936. He was an instructor in physics first at Grinnell and then at Cornell, and in 1914 joined the technical staff of Western Electric Company, from which Bell Telephone Laboratories was later derived. He has continued with that organization ever since, except during 1917–18, when he was in charge of the laboratory of the United States Army Signal Corps in Paris, France. He was made assistant director of research at Bell Laboratories in 1927, director of research in 1933, and executive vice-president in 1937. He has been active on AIEE technical committees and is a member of the Franklin Institute, American Physical Society, and American Association for the Advancement of Science.

J. P. Hogan (M'31) member of firm of Parsons, Klapp, Brinckerhoff, and Douglas, consulting engineers, New York, N. Y., has been officially nominated for president of the American Society of Civil Engineers for 1940. He was born June 12, 1891, at Chicago, Ill., and attended Harvard University, receiving the degrees of bachelor of arts (1903) and bachelor of science (1904). After about two years as assistant engineer for the New York, N. Y., Rapid Transit Commission, he was employed by the New York, N. Y., Board of Water Supply in 1906 as assistant engineer on the construction of the Catskill Aqueduct. In 1911 he became division engineer, continuing in that position until 1917. He served in France until 1919, attaining the rank of lieutenant-colonel on the United States Army General Staff, and on his return was acting deputy chief engineer for the New York Board of Water Supply. In 1920 he went with the firm of Parsons, Klapp, Brinckerhoff, and Douglas, directing the New York water power investigation 1920–23. He has been a member of the firm since 1926, specializing in power development. He was chief engineer and director of construction of the New York World's Fair 1936–39, continuing as vice-president and chief engineer consultant for the Fair Corporation. He has served as a director and vice-president of the ASCE and is a member of The American Society of Mechanical Engineers.

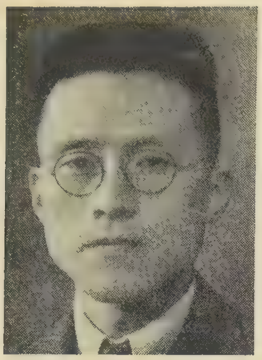
W. F. Davidson (A'14, F'26) director of research, Consolidated Edison Company of New York, Inc., New York, N. Y., and recently appointed chairman of the AIEE committee on research, has been appointed by the National Research Council to the chairmanship of its conference on electrical insulation. A biographical sketch of Mr. Davidson appeared in the October issue, page 443.

L. M. Goldsmith (M'26) chief engineer, Atlantic Refining Company, Philadelphia, Pa., has been awarded the Melville Medal of The American Society of Mechanical Engineers for his paper "High-Pressure High-Temperature Turbine-Electric Steamship J. W. Van Dyke." The medal is awarded annually for the best original paper on a mechanical-engineering subject presented to the Society. Mr. Goldsmith was born July 1, 1893, at Pottsville, Pa., and graduated in electrical engineering from Drexel Institute of Technology in 1914. During the next two years he was employed by the Perpetual Fuse Company and the United Gas Improvement Company, both of Philadelphia, and by the City of Philadelphia Department of Public Works. He went with the Atlantic Refining Company in 1916 as a mechanical draftsman, and held successively the positions of experimental engineer, engineer of tests, technical assistant to the president, and consulting engineer. In 1934 he was made manager of the general engineering and construction department, and in 1937 chief engineer. He is a vice-president and director of the Atlantic Pipe Line Company, and a director of the Atlantic Oil Shipping Company, subsidiaries of the Atlantic Refining Company. He is a member of The American Society of Mechanical Engineers, American Society of Naval Engineers, Society of Automotive Engineers, American Welding Society, and other organizations, and has made various contributions to technical literature.

M. B. Long (A'19) has been appointed assistant to the executive vice-president, Bell Telephone Laboratories, Inc., New York, N. Y., to co-ordinate the work of the various organizations and individuals concerned with building and equipping the Laboratories' new building at Murray Hill, N. J. Since graduating with the degree of bachelor of science from the University of Nebraska in 1917, Mr. Long has been assistant physicist, Bureau of Standards, Washington, D. C., 1917–19; research engineer, Western Electric Company, New York, N. Y., 1919–25; educational director, Bell Telephone Laboratories, 1925–30. Since 1930 he has been assistant director of publications. He had charge of the Bell System exhibits at the New York World's Fair, the Chicago Exposition, of 1933, and other recent expositions.

H. W. Button (A'19) has been appointed vice-president in charge of operations of the Laclede Power and Light Company, St. Louis, Mo. He was graduated in electrical engineering from Worcester Polytechnic Institute in 1912, and spent the following three years with the Westinghouse Electric and Manufacturing Company. He became assistant engineer for the Connecticut Company, New Haven, in 1915, and was later with Stone and Webster, Boston, Mass., the American Railways Company, Philadelphia, Pa., and the Eastern New Jersey Power Company, Allenhurst. He has been associated with the Laclede company for some years.

C. J. Holslag (M'19) president, general manager, and chief engineer, Electric Arc Cutting and Welding Company, Newark, N. J., has been awarded the Samuel Wylie Miller Memorial Medal of the American Welding Society. He was selected as the person who, in the judgment of the board of awards, was "most deserving for conspicuous contributions to the advancement of welding or cutting of metals." He was born at Addison, N. Y., December 13, 1885, and received the degree of electrical engineer from Columbia University in 1908. After graduation he entered the electrical-engineering department of the New York Central Railroad, later becoming resident engineer at Amsterdam, N. Y. He continued with the railroad until 1918, but was



Y. H. KU

J. A. FRENCH

C. J. HOLSLAG

also associated with organizations engaged in the manufacture of electrodes from 1915. In 1918 he took part in the organization of the Electric Arc Cutting and Welding Company, of which he became chief engineer, and later president and general manager. He holds numerous patents, is the author of the Arc Welding Handbook and other contributions to technical literature, and is a member of the American Welding Society and the American Electrochemical Society.

Y. H. Ku (A'27, M'34) was appointed vice-minister of education by the Chinese National Government in January 1938, and since that time has worked with the minister of education on China's wartime educational program. Doctor Ku, who is dean of engineering, National Tsing Hua University, Peiping, China, was born December 24, 1902, at Wusih, Kiangsu Province, China. He is a graduate of Tsing Hua College, and holds the degrees of bachelor of science in electrical engineering (1925), master of science (1926), and doctor of science (1928) from Massachusetts Institute of Technology. During 1928 he was employed for short periods at the East Springfield, Mass., works of the Westinghouse Electric and Manufacturing Company, and by the General Electric Company, Schenectady, N. Y. He returned to China in 1929 to become head of the electrical-engineering department, National Chekiang University, Hangchow, and in 1931 became dean of the engineering college, National Central University, Nanking. He went to Tsing Hua University in 1932 to establish the engineering school. He is a director of the Chinese Institute of Engineers, one of the founders of the Chinese Institute of Electrical Engineers, and author of technical papers in Chinese and in English.

J. A. French (M'22) has been appointed chief engineer of the Guardian Manufacturing and Supply Corporation, New York, N. Y. He was born August 2, 1892 at Waltham, Mass., and received the degree of bachelor of science in electrical engineering from Tufts College in 1914. Following graduation he was employed as a special apprentice in the electric locomotive shop of the New York, New Haven, and Hartford Railroad. In 1916 he entered the construction department of the power Construction Company, Worcester, Mass., and the next year was employed by the H. M. Hope Engineering Company as resident electrical engineer on construction for the Eastern Connecticut Power Company. He went with the latter company in 1919 as construction superintendent, becoming electrical engineer in 1921. When the company became the eastern division of the Connecticut Power and Light Company, in 1928, he became research engineer for the eastern division, continuing in that position until 1938. He was electrical engineer in the consulting division of Day and Zimmermann, Philadelphia, Pa., until his present appointment. He is a past-chairman of the AIEE Connecticut Section.

E. F. Heath (A'37) has been appointed senior valuation engineer, New York State Public Service Commission, New York, N. Y. He had been associated with the Brooklyn Edison Company, Brooklyn, N. Y., since 1919, having been inspector of apparatus, assistant on the survey which preceded the system's change from direct to alternating current, and later engaged in cost accounting, estimating, and engineering analyses. Since 1934 he had been engaged on valuation work for the establishment of continuing property records for the system. He was graduated in electrical engineering from New York University in 1937.

C. L. Sampson (A'25, M'39) has been appointed plant engineer, Minnesota area, Northwestern Bell Telephone Company, Minneapolis, Minn. Mr. Sampson, who holds the degrees of bachelor of science, master of science, and electrical engineer, from the University of Minnesota, had been engineer of transmission, protection, and plant extension for the Iowa area of the company since 1928, and prior to that time was an instructor in electrical engineering at the University of Minnesota, Minneapolis.

R. W. Griffiths (A'39) is now production planner, production engineering department, Boeing Aircraft Company, Seattle, Wash. He was formerly a student engineer with L. R. Teeple Company, Portland, Ore., and engaged on a power and transmission line survey for the Bonneville Dam project.

S. E. Schultz (A'25) formerly electrical engineer, Port of New York Authority, New York, N. Y., is now consulting electrical engineer, Bonneville Dam project, Portland, Ore.

J. R. Perkins, Jr. (A'36) has been appointed assistant professor of engineering physics, Grove City College, Grove City, Pa. He received the degree of electrical engineer at Princeton University in 1935, and last year held a teaching fellowship in physics at Massachusetts Institute of Technology, Cambridge.

W. S. Gifford (A'16) president, American Telephone and Telegraph Company, New York, N. Y., is a member of the committee of industrialists appointed by the National Association of Manufacturers to select outstanding inventors to be honored as "Modern Pioneers."

W. M. Bauer (A'29, M'36) has been appointed lecturer in electrical engineering, University of Minnesota Institute of Technology, Minneapolis. He was formerly instructor in electrical engineering at Northwestern University, Evanston, Ill., and since 1937 has been a graduate student and teaching assistant at Harvard University, Cambridge, Mass.

David Sarnoff (M'23) president, Radio Corporation of America, New York, N. Y., is a member of the committee of industrialists appointed by the National Association of Manufacturers to select outstanding inventors to be honored as "Modern Pioneers."

J. A. Mathews (A'37) formerly junior engineer with the United States Bureau of Mines, Central Experiment Station, Pittsburgh, Pa., is now a junior patent engineer, United States Patent Office, Washington, D. C.

A. B. Hallman (A'38) formerly in charge of aircraft electrical maintenance at the Newark station of Eastern Air Lines, Inc., is now an electrical technician for the company at Miami, Fla.

T. C. Smith (A'37) formerly range engineer, Edison General Electric Appliance Company, Chicago, Ill., is now engineer in charge of electric-range section, appliance division, Stewart Warner Corporation, Indianapolis, Ind.

F. J. McDonald (A'38) is now an electrical engineer in the office of the Quartermaster General, construction division, War Department, Washington, D. C. He was formerly in the electrical design department, E. I. du Pont de Nemours and Company, Wilmington, Del.

O. T. Mundt (A'37) formerly transformer tester for Westinghouse Electric and Manufacturing Company, Sharon, Pa., is now an engineer at the East Pittsburgh works of the company.

W. G. Whitsitt (A'35) formerly sales engineer, technical department, Leeds and Northrup, Philadelphia, Pa., is now design engineer, for the Breeko Corporation, Nashville, Tenn.

M. F. Davis (A'37) formerly in the United States Engineer's office, Huntington, W. Va., is now a designer for the National Advisory Committee for Aeronautics, Langley Field, Va.

C. C. Diemond (A'37) formerly junior engineer, United States Bureau of Reclamation, Ephrata, Wash., is now employed in the same capacity on the Bonneville Dam project, Portland, Ore.

C. R. Vail (A'38) has been appointed instructor in electrical engineering at Duke University, Durham, N. C. He was formerly employed by the General Electric Company and was operating engineer for the company's high-voltage demonstration at the New York World's Fair.

J. T. Holmes (M'24) formerly president, Mitchell-Vance Company, New York, N. Y., is again associated with the Frink Corporation, Long Island City, N. Y., where he will continue to work with special lighting. He had formerly been chief engineer of that company for many years.

D. F. Smith (A'31, M'35) formerly chief engineer, State of Oregon, Pacific Telephone and Telegraph Company, Portland Ore., has been transferred by the company to the position of chief engineer, Northern California and Nevada, with headquarters at San Francisco, Calif.

S. E. Warner (A'31) formerly chief engineer, radio station WBRY, Waterbury, Conn., has been appointed supervisor of the Connecticut State Police radio system. Before going to WBRY he was instructor in electrical engineering at Rensselaer Polytechnic Institute, Troy, N. Y.

K. L. Howe (A'28, F'36) has been transferred by Westinghouse Electric and Manufacturing Company from the position of district engineer, Seattle, Wash., to that of engineer, San Francisco, Calif. He has been secretary of the AIEE Seattle Section.

H. C. Wolfe (A'28) formerly rural electrification representative, Monongahela West Penn Public Service Company, Fairmont, W. Va., is now manager of the O and A Electric Co-operative, Newaygo, Mich., which serves ten counties in that area.

A. J. Fischer (A'36) is now metallurgist with the Frith-Sterling Steel Company, McKeesport, Pa. He was formerly with American Cutting Alloys, Lewiston, Maine, and the Metal Powders Processing Company, Inc., Long Island City, N. Y.

M. B. Marshall, Jr. (A'37) formerly student engineer, General Electric Company, Schenectady, N. Y., is now an electrical engineering assistant, New York City Board of Transportation, power division, New York, N. Y.

S. M. Wilson (A'27, M'34) has been appointed assistant engineer of manufacture, Western Electric Company, New York, N. Y. He was formerly manager of the company's central office division at Kearny, N. J.

Leif Holst (A'23, M'30) formerly designer, American Gas and Electric Service Corporation, New York, N. Y., is now an associated electrical engineer with the ordnance bureau of the United States Navy Department, Washington, D. C.

S. H. Hanville, Jr. (A'39) formerly assistant manager, Ashland Lumber and Supply Company, Newcomerstown, Ohio, is now employed by the Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

L. B. Bryan (A'35) formerly steam engine and generator operator, Joseph E. Seagram and Sons, Inc., Lawrenceburg, Ind., is now a junior agricultural engineer for the soil conservation service, United States Department of Agriculture, Ottawa, Kans.

S. E. Clements (A'39) has been appointed instructor in electrical engineering at the University of Kansas, Lawrence. He formerly held a similar position at Iowa State College, Ames.

L. D. Harris (A'38) is now instructor in electrical engineering, University of Utah, Salt Lake City. He received the degree of master of science in electrical engineering from Purdue University in 1939.

R. B. Capron (A'30, M'35) has been made distribution engineer for the Central New York Power Corporation, with headquarters at Watertown, N. Y. He was formerly district engineer, Potsdam, N. Y.

G. C. Morris (A'35) formerly junior electrical engineer, Sunbeam Electric Manufacturing Company, Evansville, Ind., has been employed as an engineer by the A. G. Redmond Company, Owosso, Mich.

A. M. Dayton (A'39) formerly assistant field engineer, E. B. Badger and Sons Construction Company, Toledo, Ohio, is now associated with the Howell Electric Motor Company, Howell, Mich.

J. J. Thomason (A'19, M'25) has been transferred to Memphis, Tenn., by the Westinghouse Electric and Manufacturing Company. He was formerly manager of central station sales, St. Louis, Mo.

J. S. Lieb (A'35) formerly a student engineer at Allis-Chalmers Manufacturing Company, Milwaukee, Wis., has been transferred to the New York office of the company as representative.

C. R. Hine (A'37) formerly special apprentice, Pennsylvania Railroad Company, Philadelphia, Pa., is now instructor in industrial arts, East Side High School, Newark, N. J.

A. P.-T. Sah (A'35, M'36) president of Amoy University, is now located at Changting, Fukien Province, China, where the university has been removed because of war conditions.

G. A. Palka (A'39) formerly junior engineer, Federal Emergency Administration of Public Works, Chicago, Ill., is now employed as a designing engineer by the Standard Transformer Company, Warren, Ohio.

A. S. Anderson (A'26, M'38) formerly assistant superintendent of distribution, New Orleans Public Service, Inc., New Orleans, La., is now associated with Ebasco Services, Inc., New York, N. Y.

D. R. Frantz (A'39) formerly junior engineer, Washington Institute of Technology, College Park, Md., is now employed by the Bell Telephone Laboratories, Inc., New York, N. Y.

M. M. Belknap (A'31) formerly electrical inspector, Iowa Electric Light and Power Company, Perry, has been transferred to Cedar Rapids as chief electrician, Boone district.

R. S. Taylor (A'36) formerly division engineer, Oklahoma Gas and Electric Company, Muskogee, is now with the district engineering department of the company at Oklahoma City.

C. R. Dixon (A'35) formerly assistant plant engineer, Mount Hope Finishing Company, North Dighton, Mass., is now electrical engineer, The Aluminum Company of America, New Kensington, Pa.

R. A. Krasovec (A'31) formerly a switchboard operator for the Tennessee Valley Authority at Norris, Tenn., is now electrician, International Smelting and Refining Company, Perth Amboy, N. J.

H. F. Danneman, Jr. (A'32) formerly industrial engineer, Tide Water Associated Oil Company, New York, N. Y., has been transferred to Boston, Mass., as regional engineer for the company.

S. V. Swanson (A'30) formerly chief engineer, Refrigerator Appliances, Inc., Chicago, Ill., is now associated with the engineering department of the Young Radiator Company, Racine, Wis.

C. R. Delagrange (A'30) formerly foreman, Firestone Rubber and Latex Products Company, Fall River, Mass., is now a textile engineer, Celanese Corporation of America, Narrows, Va.

R. B. Harder (A'38) formerly instructor in electrical engineering, Iowa State College, Ames, is now employed as electrical test engineer by the Electromotive Corporation, LaGrange, Ill.

W. W. Winter (A'38) formerly a student apprentice with Allis-Chalmers Manufacturing Company, West Allis, Wis., has been appointed sales engineer for the company's Chicago, Ill., office.

R. E. Lyle (A'32) formerly electrical engineer, Department of Utilities, City of Tacoma, Wash., is now junior engineer, Bonneville Dam project, Portland, Ore.

G. W. Bills (A'38) former graduate teaching assistant at the University of Washington, Seattle, is now a junior electrical engineer, Bonneville Dam project, Portland, Ore.

T. B. Jones, Jr. (A'35) formerly with the Linde Air Products Company, Pittsburgh, Pa., is now with Commonwealth and Southern Corporation, Jackson, Mich.

E. C. Hillman (A'37) has been appointed a junior engineer, for the ordnance bureau, United States Navy Department, Washington, D. C.

C. J. Eiwen (A'39) is now a junior welding engineer for the ordnance bureau, United States Navy Department, Washington, D. C.

O. L. Worden (A'35) formerly valuation engineer, New York State Electric and Gas Company, Ithaca, N. Y., is now employed by Crouse-Hinds Company, Syracuse, N. Y.

B. H. Ormson (A'22) formerly chief electrician, Ruby Gulch Mining Company, Zortman, Mont., is now employed by the Montana Power Company, Cut Bank.

D. H. Shick (A'39) is now a graduate teaching assistant at the University of Missouri, Columbia, while working toward a master's degree.

H. W. von Dohlen, Jr. (A'36) formerly radio operator, Station WRUF, Gainesville, Fla., is now associated with Eastern Air Lines, Inc., Miami, Fla.

E. R. Groo (A'35) formerly supervisor, Aerovox Corporation, Brooklyn, N. Y., is now cash control manager, Consolidated Molded Products Company, Scranton, Pa.

M. P. Naab (A'32) formerly general construction supervisor, Wisconsin Telephone Company, Appleton, has been appointed district plant manager at Appleton.

W. L. Cassell (A'25) formerly associate professor of electrical engineering, University of Colorado, Boulder, now holds the same position at Iowa State College, Ames.

U. N. Halliday (A'28) sales engineer, Pacific Electric Manufacturing Company, has been transferred from San Francisco, Calif., to Denver, Colo.

R. P. Posey (A'39) is now junior electrical engineer, Rudolph Wurlitzer Manufacturing Company, North Tonawanda, N. Y.

V. E. Gardner (A'39) is now an instructor in electrical engineering, University of North Dakota, Grand Forks.

Jules Cohen (A'39) is now employed as a junior electrical engineer on the Bonneville Dam project, Portland, Ore.

S. R. Hribar (A'38) has been employed as an electrical draftsman by Commonwealth and Southern Corporation, Jackson, Mich.

H. C. Green (A'39) is now employed in construction engineering for the Carolina Power and Light Company, Southern Pines, N. C.

Obituary

Edwin Dow Wood (A'21, M'26) general superintendent, Louisville Gas and Electric Company, Louisville, Ky. and vice-president 1937–39 of AIEE District 4, died November 9, 1939. He was born April 17, 1878, at Montevideo, Uruguay, and attended high school at Callao, Peru. He received the degree of bachelor of science from DePauw University in 1903, and during 1903 and 1904 was employed by the Union Traction Company of Indiana, the Pope-Waverly Electric Company, Indianapolis, and the General Electric Company, Schenectady, N. Y. In 1904 he went to Lima, Peru, in the employ of the Empresa Electrica Santa Rosa, which later became the Empresas Electricas Associadas. He became assistant chief engineer and was acting chief engineer when he resigned in 1909 to become construction department foreman for General Electric at Schenectady. In the following year he was transferred to the engineering department of the Cincinnati district. He became electrical operating engineer for the Louisville Gas and Electric Company in 1915, was made electrical engineer in 1926, and general superintendent in 1932. He was elected a director of the company in 1936. He was a past-chairman of the AIEE Louisville Section, and completed his term as vice-president of District 4 August 1, 1939. He was also a past-president of the Engineers and Architects Club of Louisville.

Paul N. Nunn (A'95, M'96) president, Telluride Power Company, Salt Lake City, Utah, died October 27, 1939 at San Diego, Calif. He was born at Medina, Ohio, July 31 1860, educated for the teaching profession, and engaged in teaching and business until 1890. He then went to Telluride, Colo., becoming consulting engineer for the San Miguel Consolidated Gold Mining and Gold King Mining companies. In connection with the development of electric power for mines, he was associated with the design and installation of one of the first commercial high-voltage a-c transmission systems to be put into successful operation. The Telluride Power Company, which grew out of that enterprise, built the first electric power plant in Utah in 1897, and for a time was said to be the most powerful high-voltage system in the world. It extended through Colorado, Montana, Utah, and Idaho. Mr. Nunn was chief engineer for the company 1890 to 1910, and during 1903–10 was also chief engineer for the Ontario Power Company, Niagara Falls. In 1917 he became president and director of the present Telluride Company, continuing to hold that position until his death, although he had been retired from active professional work for some time. He was a member of the American Society of Civil Engineers and The American Society of Mechanical Engineers.

William Kemp Vanderpoel (A'08, M'21) vice-president, The Okonite Company, and the Okonite Callender Cable Company, New York, N. Y., died October 21, 1939 at South Orange, N. J. He was born at New York, May 29, 1880, and educated there. His engineering career began as cadet engineer on trans-Atlantic liners in 1897, after which he was engaged in shop electrical work; on drafting for the New York and New Jersey Telephone Company; in mining in South America; with the Red Telephonica Company, then operating in Havana, Cuba; as a bank cashier in New York; and as assistant purchasing agent for the Florida East Coast Railroad and Hotel companies. In 1908 he became associated with the Public Service Electric Company, Newark, N. J., as division superintendent, becoming general superintendent in 1916. He was appointed vice-president and executive engineer of The Okonite Company in 1926. He was a manager of the AIEE 1923–27, and had been active on technical committees. He was also active in the National Electric Light Association, and was the author of many technical papers.

Gerald John Wagner (M'20) president, G. J. Wagner and Company, consulting engineers, Grand Rapids, Mich., died September 2, 1939. Born in Grand Rapids May 28, 1886, he received the degree of bachelor of science in electrical engineering from the University of Michigan in 1910. He was employed by the Metropolitan Street Railway Company, New York, N. Y., and the Grand Rapids-Muskegon Power Company, before becoming an electrical engineer for the Michigan Railway Engineering Company, Kalamazoo, in 1912. In that capacity and as general superintendent he was engaged in construction of electric third-rail lines until 1916, when he became superintendent of construction for the Michigan Railway Company, Jackson. He was city engineer for the City of Grand Rapids 1918–19, becoming director of public service for the City in 1920. In 1922 he established his own private consulting firm, acting as consultant for the City of Grand Rapids and other cities, Michigan Public Utilities Commission and other state commissions, and for various utility and industrial companies. He was a member of The American Society of Mechanical Engineers, past-president, Michigan Engineering Society, and member of local engineering organizations.

William R. Garton (A'05) retired, Pelham, N. Y., died September 15, 1939. He was born July 20, 1868, and studied electrical engineering at Coe College. In 1889 he began electrical railway construction work at Des Moines, Iowa, under the Thomson Houston Electric Company, later becoming associated with the Mississippi Valley Construction Company in charge of work at Keokuk, Iowa. In about 1892 he formed a company for the manufacture of the Garton lightning arrester, which he had invented and patented. In 1894 he became manager of the railway department of the Central Electric Company, Chicago, Ill., and in 1898 organized the W. R. Garton Company, acting as president and general manager. About ten years later he became general manager of the manufacturing department, Lord Electric Company, New York, N. Y., and later vice-president. He was afterward sales engineer for various companies, including the Edison Storage Battery Company. He was long associated with the Walker Vehicle Company, New York, as sales engineer, assistant to the eastern district manager, and later New York district sales manager. During the early 1930's he was vice-president of the Sarvas Electric Company, New York.

Walter Robinson McRae (M'17) superintendent of rolling stock and shops, Toronto Transportation Commission, Toronto, Ontario, Canada, died during the summer of 1939, according to information recently received at Institute headquarters. He was born at Bolsover, Ont., October 21, 1874, and educated in Toronto. From 1887 to 1891 he was employed successively for short periods by various electric companies in Toronto and St. Catherine's, and during 1891–92 was with the electrical department of Bennett and Wright Company, Toronto, installing equipment in the Toronto School of Science. In 1892 he was engaged to supervise installation of car equipment in the electrification of the Toronto Railway Company, and continued in the employ of that and associated companies for many years, on railway electrification, installation, and op-

eration of power plant equipment. In 1904 he became general foreman of shops of the Toronto Railway Company, and in 1907 master mechanic in charge of rolling stock and shops. About 15 years later he went with the Toronto Transportation Commission.

Johnston Livingston (A'98) head of the engineering and construction firm of his name, New York, N. Y., died October 10, 1939. He was born in New York, December 19, 1876, and was graduated from Columbia University in 1898 with the degree of electrical engineer. The following year he was associated in the formation of the United Engineering and Contracting Company, New York, of which he was vice-president, and in 1900 he formed the firm of Johnston Livingston, Jr., and Company, electrical engineers and contractors. That organization was incorporated in 1909 as J. Livingston and Company, with Mr. Livingston as president, and engaged in a wide variety of electrical and power plant engineering and construction. About 14 years ago he left that company to form the construction firm of Johnston Livingston which he headed until his death.

David Humphrey Johnston, Jr. (M'28) assistant electrical engineer, Consolidated Gas, Electric Light, and Power Company, Baltimore, Md., died September 12, 1939, in Chicago, Ill. He was born at Baltimore, November 24, 1891, and received the degree of bachelor of science in electrical engineering from Johns Hopkins University in 1916. He was a second lieutenant in the United States Army Engineering Corps during 1917–19, and assistant to the director of terminal facilities for the American Expeditionary Forces. In 1919 he was employed as inspector by the Consolidated Gas, Electric Light, and Power Company, Baltimore. He became superintendent of electrical service in 1922, with responsibility for all electrical construction, and had recently become assistant electrical engineer.

Louis Homer Bayha (A'33) industrial service man, central manufacturing district, Leeds and Northrup Company, Los Angeles, Calif., died September 17, 1939, at San Gabriel, Calif. He was born September 14, 1909, in Los Angeles, and received the degree of bachelor of science in electrical engineering in 1932 from the University of Southern California. During his senior year he acted as laboratory assistant in electrical engineering. After graduation he was employed as junior electrician by the Shell Company of California, Wilmington, for about two years, before going to Leeds and Northrup as industrial engineer. He was a member of Eta Kappa Nu.

Lloyd Gilroy Hagenbuch, Jr. (A'34) junior engineer, Ford Instrument Company, Inc., Long Island City, N. Y., died October 1, 1939. He was born June 4, 1912, at New York, N. Y., and was graduated from Rensselaer Polytechnic Institute in 1933 with the degree of electrical engineer. He was employed for some months in the engineering division of the Federal Emergency Administration of Public Works, Washington, D. C., and by Gibbs and Hill, consulting engineers, on the electrification of the Wilmington-Washington route of the Pennsylvania Railroad, before going with the Ford Instrument Company. He was a member of Sigma Xi.

John Tak Sang Fung (A'35) administration engineer, Kwangtung Wireless Administration, Canton, China, died some months ago, according to information recently received at Institute headquarters. He was born at Kohala, Hawaii, March 16, 1909, and received the degree of bachelor of science in electrical engineering at Heald Engineering College in 1933. He designed and constructed a low-power broadcast transmitter for the Chinese Government during 1933–34, and in 1934 was appointed by the Government to the position he held at the time of his death. He was a member of the Institute of Radio Engineers.

Membership

Recommended for Transfer

The board of examiners, at its meeting on November 16, 1939, recommended the following members for transfer to the grade of membership indicated. Any objection to these transfers should be filed at once with the national secretary.

To Grade of Fellow

Hazeltine, H. L., engineer of insulation, The Sterling Varnish Company, Haysville, Pa.
Homan, J. G., private research, writing, 8700 Ventnor Avenue, Atlantic City, N. J.
Kalb, W. C., advertising department, National Carbon Company, Cleveland, Ohio.
Miner, D. F., professor, Carnegie Institute of Technology, Pittsburgh, Pa.
Wildes, K. L., associate professor of electrical engineering, Massachusetts Institute of Technology, Cambridge.

5 to Grade of Fellow

To Grade of Member

Beavers, M. F., development engineer, General Electric Company, Pittsfield, Mass.
Farry, O. T., transformer engineer, Wagner Electric Corporation, St. Louis, Mo.
Gross, I. W., electrical research engineer, American Gas and Electric Service Corporation, New York, N. Y.
McKibben, W. E., research engineer, General Electric Company, Schenectady, N. Y.
Mittag, A. H., electrical engineer, General Electric Company, Schenectady, N. Y.
Morey, C. V., assistant meter engineer, Consolidated Edison Company of New York, Inc., New York, N. Y.
Sasscer, W. H., engineer in electrical & mechanical department, Pennsylvania Coal Company, Scranton.
Vaughan, C. F., assistant head of estimate section, General Electric Company, Pittsfield, Mass.

8 to Grade of Member

Applications for Election

Applications have been received at headquarters from the following candidates for election to membership in the Institute. Names of applicants in the United States and Canada are arranged by geographical Districts. If the applicant has applied for direct admission to a grade higher than Associate, the grade follows immediately after the name. Any member objecting to the election of any of these candidates should so inform the national secretary before December 31, 1939, or February 29, 1940, if the applicant resides outside of the United States or Canada:

United States and Canada

1. North Eastern

Connors, J. J., Irvington Varnish and Insulator Company, Irvington, N. J.
Hershey, J. D., General Electric Company, Buffalo, N. Y.
Lazur, E., Babcock Printing Press Corporation, New London, Conn.
Martin, H. W., Allis-Chalmers Manufacturing Company, Boston, Mass.
McLachlan, W. J., General Electric Company, Schenectady, N. Y.
Mortimer, A. J., Federal Shipbuilding and Dry Dock Company, Kearny, N. J.
Murphy, H. E. (Member), Stone and Webster Engineering Corporation, Boston, Mass.
Ripley, E. B., Jr., Connecticut Light and Power Company, Devon, Conn.
Schifino, A. G., Rochester Radio Supply Company, Rochester, N. Y.
Shott, H. S., General Electric Company, Schenectady, N. Y.
Sisterhenm, M. L. (Member), Danbury and Bethel Gas and Electric Light Company, Danbury, Conn.
Stubbs, S. R., Allis Chalmers Manufacturing Company, Boston, Mass.

2. Middle Eastern

Bond, G. W., Ohio Public Service Company, Massillon.
Bourne, R. F. (Member), Federal Power Commission, Washington, D. C.
Carlson, E. G., Bell Telephone Company of Pennsylvania, Philadelphia.
Cunningham, W. A., 831 Linden Street, Allentown, Pa.
Dimity, C. D., Copper Wire Engineering Association, Washington, D. C.
Eldridge, G. C., Jr., Bell Telephone Company of Pennsylvania, Philadelphia.
Filler, C. F., Consolidated Gas Electric Light and Power Company of Baltimore, Baltimore, Md.
Hyer, R. J. (Member), Copper Wire Engineering Association, Washington, D. C.
Kelly, W. J., Bell Telephone Company of Pennsylvania, Philadelphia.
Keneipp, H. E., Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.
Kutcher, W. J., Electric Controller and Manufacturing Company, Cleveland, Ohio.
Lory, M. R., Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.
Moss, C. R., Hickok Electrical Instrument Company, Cleveland, Ohio.
Prasse, H. E. (Member), Atlantic Refining Company, Philadelphia, Pa.
Sherburne, R. G., Bliss Electrical School, Takoma Park, D. C.
Smith, R. C., Philadelphia Electric Company, Philadelphia, Pa.
Standring, J. M., Jr., Bell Telephone Company of Pennsylvania, Philadelphia.
Steiner, L. S., Timken Roller Bearing Company, Canton, Ohio.
Steinert, W. E., Bell Telephone Company of Pennsylvania, Philadelphia.
Stover, H. N. (Member), Philco Radio and Television Corporation, Philadelphia, Pa.

3. New York City

Gunnison, G. S., Consolidated Car Heating Company, Inc., New York, N. Y.
Levine, S., 1136 Metcalf Avenue, Bronx, New York, N. Y.
Li, T. C., 1017 John Jay Hall, Columbia University, New York, N. Y.
Loomis, S. D. (Member), Federal Advisers, Inc., New York, N. Y.
Mapes, C. M. (Member), American Telephone and Telegraph Company, New York, N. Y.
Palmer, W., Sperry Gyroscope Company, Brooklyn, N. Y.
Quinn, J. L., Jr., National Broadcasting Company, New York, N. Y.
Sordon, E. P., Consolidated Edison Company, New York, N. Y.
Sowards, C. C., Copperweld Steel Company, New York, N. Y.
Stainken, H. A., Otis Elevator Company, New York, N. Y.

4. Southern

Allebaugh, R. O., Virginia Public Service Company, Harrisonburg.
Bodemuller, H. R. (Member), Louisiana Public Utilities Company, Inc., Lafayette.
Harrington, J. L., Southwestern Gas and Electric Company, Shreveport, La.
Murphree, D. D., Southwestern Gas and Electric Company, Shreveport, La.
Tarwater, C. E., Knoxville Electric Power and Water Board, Knoxville, Tenn.
Underwood, J. L., Jr., 302 Walton Building, Atlanta, Ga.
Vannort, B. O. (Member), 1207 Commercial Bank Building, Charlotte, N. C.

5. Great Lakes

Boyajian, J. A., J. P. Seeburg Corporation, Chicago, Ill.
Carter, B. H., Sanitary District of Chicago, Chicago, Illinois.
Cox, I. W. (Member), Cutler Hammer, Inc., Milwaukee, Wis.
Cross, R. D., Detroit Institute of Technology, Detroit, Mich.
DeFrees, J. T., Hubbard and Company, Cicero, Ill.
Ferguson, P. M., Jr., American Automatic Electric Sales Company, Alexandria, Minn.
Gadler, S., Rural Electrification, St. Paul, Minn.
Lockwood, R. R., Brown, Jackson, Boettcher, and Dienner, Chicago, Ill.
Marple, C. N., Iowa Public Service Company, Sioux City.
Northcott, J. A., Jr. (Member), University of Notre Dame, Notre Dame, Ind.
Stauder, L. F. (Member), University of Notre Dame, Notre Dame, Ind.
Stiles, K. P. (Member), American Telephone and Telegraph Company, Springfield, Ill.
Witty, B. G., Allis Chalmers Manufacturing Company, Milwaukee, Wis.

7. South West

Duncan, C. C., American Telephone and Telegraph Company, Dallas, Tex.
Hubbell, M. F. (Member), Union Electric Company of Missouri, St. Louis.
Laisure, T. H., Kansas Gas and Electric Company, Wichita.
Lienesch, J. P., Southwestern Bell Telephone Company, St. Louis, Mo.
Martin, D. D., Southwestern Light and Power Company, Lawton, Okla.
Mathers, R. S., Oklahoma Gas and Electric Company, Oklahoma City.
McClain, O. E., Southwestern Bell Telephone Company, Oklahoma City, Okla.
Milligan, B., Oklahoma Gas and Electric Company, Oklahoma City.
Peacock, V., Dallas Power and Light Company, Dallas, Tex.
Wallace, P. G., Texas Power and Light Company, Dallas.
Ward, D. S., United Light and Power Service Company, Kansas City, Mo.

8. Pacific

Hewlett, W. R., Hewlett Packard Company, Palo Alto, Calif.
Pettengill, C. L., Public Works Administration, San Francisco, Calif.
Ralston, E L., Jr., Westinghouse Electric and Manufacturing Company, San Francisco, Calif.
von Seeburg, A. L., Pacific Electric Manufacturing Corporation, San Francisco, Calif.

9. North West

Eastman, A. V. (Member), University of Washington, Seattle.
Gogins, J. F., General Electric Company, Spokane, Wash.
Munakata, Y., Puget Sound Power and Light Company, Seattle, Wash.
Ross, J. S. (Member), Bonneville Project, Seattle, Wash.

10. Canada

Brown, J. A., Jr., Bell Telephone Company of Canada, Toronto, Ont.
Hardie, R. C., Canadian General Electric, Ltd., Vancouver, B. C.

Total, United States and Canada, 83

Elsewhere

Malkani, H. U., Indian Cable Company, Ltd., Bombay, India.
Roy, J. K. (Member), British Insulated Cables, Ltd., Calcutta, India.

Total elsewhere, 2

Addresses Wanted

A list of members whose mail has been returned by the postal authorities is given below, with the addresses as they now appear on the Institute record. Any member knowing of corrections to these addresses will kindly communicate them at once to the office of the secretary at 33 West 39th St., New York, N. Y.

Crawford, Wade P., Box 364, Coeur D'Alene, Idaho.
Doherty, Joseph F., 901 Hill St., Wilkinsburg, Pa.
Evans, David T., Box 194, Anyox, B. C., Can.
Hollifield, Ray, 5118 Milam St., Dallas, Texas.
Keiser, M., 1841 Broadway, New York, N. Y.
Lovett, Morris, Diehl Manufacturing Company, Elizabethport, N. J.
O'Fiel, J. C. Dudley, Jr., 1214 Chartres St., Houston, Texas.
Pyne, Arnold N., 627 Third St., Niagara Falls, N. Y.
Sanchez, Hector M., 12 De Diegos Ave., Santurce, Puerto Rico.
Shimp, Robert P., 3749 N. Gratz St., Pittsburgh, Pa.
Taylor, Richard V., Hotel Wood, Jefferson & Clinton St., Syracuse, N. Y.

11 Addresses Wanted

Engineering Literature

New Books in the Societies Library

Electrical engineers may be interested in the following new books, which are among those recently received at the Engineering Societies Library, New York, N. Y. Unless otherwise specified, books listed have been presented by the publishers. The Institute assumes no responsibility for statements made in the following summaries, information for which is taken from the preface of the book in question.

RADIO INTERFERENCE SUPPRESSION. By G. W. Ingram. London, Electrical Review, 1939. 154 pages, illustrated, 8 by 5 inches, cloth, 5s. Written to provide a link between the activities of the power-plant engineer and the radio and communication systems engineer. The various causes of interference with radio reception from electrical equipment are classified in such a way, both in the text and the appendices, that the type and probable cause of interference can be readily recognized, and the method of remedy easily selected.

INDUSTRIAL SOLVENTS. By I. Mellan. New York, Reinhold Publishing Corporation, 1939. 480 pages, illustrated, 9 by 6 inches, cloth, $11.00. The first six chapters discuss the theory of solution and the general properties of solvents. The following chapters discuss the use of solvents in industries and provide specific information about available solvents, arranged under the following classes: hydrocarbons, halogenated hydrocarbons, alcohols, aldehydes, acids, ketones, ethers, esters, and plasticizers. Many diagrams and tables.

BIG BUSINESS AND RADIO. By G. L. Archer. New York, American Historical Company, 1939. 503 pages, illustrated, 10 by 6 inches, cloth, $4.00. A companion volume to the author's "History of Radio to 1926," the present book overlaps the former as a result of the inclusion of a large amount of new material concerning the struggle in the early 1920's among the groups interested in the field. Continuing from there the story covers subsequent developments and disturbances in connection with talking pictures, television, facsimile, and the general expansion of radio broadcasting. Free use is made of direct quotation, and the part played by various personalities has received full consideration.

APPLIED ECONOMICS FOR ENGINEERS. By B. Lester. New York, John Wiley and Sons, 1939. 464 pages, diagrams, etc., 9 by 6 inches, cloth, $4.00. Provides an introduction to the practical aspects of economics, based upon the conditions and problems encountered in engineering practice. Stressing the importance of habitual reference to current technical literature the author presents practical information on industrial relations and organization, standards, statistical and accounting methods, development and production problems, costs, markets, distribution, and sales.

COMPANY PLANS FOR EMPLOYEE PROMOTIONS. By H. Baker. Princeton, N. J., Industrial Relations Section of Princeton University, 1939. 48 pages, charts, 10 by 7 inches, paper, 75¢. This pamphlet presents a brief analysis of the promotion programs of representative industrial companies. General procedures in promotional programs, training for promotion, publicity on programs and opportunities and the effect of promotional plans are discussed.

ELECTRICAL ENGINEERING. By E. E. Kimberly. Scranton, Pa., International Textbook Company, 1939. 324 pages, illustrated, 9 by 5 inches, flexible, $2.75. Written specifically for the use of engineering students not majoring in electrical engineering, this textbook presents a comprehensive survey of fundamental theories, practices, and machinery. The information presented has been chosen to assist the engineer in the intelligent selection of equipment and in dealing with electrical problems.

ELECTRICITY AND MAGNETISM. By J. B. Whitehead. New York and London, McGraw-Hill Book Company, 1939. 221 pages, diagrams, etc., $8^1/_2$ by $5^1/_2$ inches, cloth, $3.00. A compact presentation of the physical and mathematical theories, intended for fourth-year undergraduate students of physics and electrical engineering. The text follows the approximate chronological order of the classical development of electrical and magnetic science, and aims to meet the needs of those who must acquire the essentials of the theory in a relatively short time.

ELEKTRISCHE HÖCHSTSPANNUNGEN. (Technische Physik in Einzeldarstellungen, number 1). By A. Bouwers; edited by W. Meissner and G. Holst. Berlin, Julius Springer, 1939. 333 pages illustrated, $9^1/_2$ by 6 inches, cloth, 31.20 rm., bound; 29.40 rm., paper. A convenient summary of present-day developments in the production and use of high voltages (of the order of several hundred thousand volts) is supplied by this monograph. The production of such voltages is described and the resulting electrical fields discussed. Chapters are devoted to insulators, to the elements of plants, and the measurement of high voltages. Finally, applications in power transmission, materials testing, radioactivity, etc., are reviewed. Bibliography of over 300 articles.

HEAT POWER. By E. B. Norris and E. Therkelsen. Second edition. New York and London, McGraw-Hill Book Company, 1939. 432 pages, illustrated, 9 by 6 inches, cloth, $4.00. The major topics of the internal-combustion engine, steam engines, steam turbines, and boiler furnaces are presented in a simple manner, including theory, analysis of heat-cycles and performance, accessories and auxiliaries. This revised edition includes a chapter on refrigeration. Many numerical examples and problems help the student in his practical application of the information. The work varies the usual arrangement by commencing with the internal-combustion engine.

IONS, ELECTRONS, AND IONIZING RADIATIONS. By J. A. Crowther. Seventh edition. New York, Longmans, Green and Company; London, Edward Arnold and Company, 1939. 348 pages, illustrated, 9 by 6 inches, cloth, $4.00. The recent developments in the field of atomic physics are presented in this textbook, for students who have covered the elementary courses. The mathematical treatment has been simplified in order to facilitate a better general understanding of the principles involved. Ionic charges, photo-electricity, atomic structure, and the various kinds of rays and radiations are discussed. Each chapter has a brief bibliography.

NOTIONS D'ÉCLAIRAGISME. By A. Salomon. Paris, Dunod, 1939. 189 pages, illustrated, 10 by 7 inches, paper, 58 frs.; bound 78 frs. The physical and physiological factors important in satisfactory lighting installations, the production of light, lighting equipment, and the proper illumination of residences and public buildings are covered in this textbook, which is intended to give architects, artists, and decorators a knowledge of the possibilities of modern technique.

PHYSICS. By E. Hausmann and E. P. Slack. Second edition. New York, D. Van Nostrand Company, 1939. 756 pages, illustrated, 9 by 6 inches, cloth, $4.00. Aims to present the essentials of physics to college students majoring in science or engineering. It attempts to give a gradual, logical approach to the subject, and to develop and illustrate clearly the fundamental concepts. The new edition has been revised, rearranged, in the light of teaching experience, and provided with many additional problems.

Engineering Societies Library

39 West 39th Street, New York, N. Y.

MAINTAINED as a public reference library of engineering and the allied sciences, this library is a co-operative activity of the national societies of civil, electrical, mechanical, and mining engineers.

Resources of the library are available also to those unable to visit it in person. Lists of references, copies or translation of articles, and similar assistance may be obtained upon written application, subject only to charges sufficient to cover the cost of the work required.

A collection of modern technical books is available to any member residing in North America at a rental rate of five cents per day per volume, plus transportation charges.

Many other services are obtainable and an inquiry to the director of the library will bring information concerning them.

Transactions

Papers and Discussions Comprising Pages 307–70 of the 1939 Volume

Low-Gas-Pressure Cable

G. B. SHANKLIN
MEMBER AIEE

DURING the past several years the company with which the writer is associated has been conducting an extensive research and engineering study of the possibilities of high-voltage cable utilizing gas as a pressure medium. The purpose has been to develop a practical cable system of this type and determine its best field of usefulness from an engineering and economic standpoint. The results of this work to date will be outlined in the present paper.

Since the need of high-voltage underground transmission arose some 40 years ago there has been and will always be a continual effort to obtain better and more economical designs of cable systems. Out of these years of experience certain principles and trends have been established. Of all the different insulating materials available, oil- or compound-impregnated paper is recognized as the best and most economical for cable rated 22 kv and above. It is also widely used at lower voltages. All of the various high-voltage cable systems that have been generally used, or subjected to full-size experimental field trial, in recent years have utilized impregnated paper as the basic insulation.

The cable systems mentioned utilize impregnated paper in different ways to accomplish certain desired results, but all of them follow, or try to follow, the same fundamental principles. These are:

(*a*). Use materials that are chemically and physically stable and of low dielectric loss and high dielectric strength.

(*b*). Eliminate all traces of impurities initially and prevent entrance of such impurities during shipment, field installation, and service.

(*c*). Prevent progressive ionization deterioration at all times.

Experience has demonstrated that the first step in accomplishing these results involves vacuum drying and impregnation under pressure after fabrication of the cable. Uniformly perfect initial impregnation is the only known method of obtaining full control of cable quality and economically meeting the requirements outlined. Incomplete initial impregnation in the factory leads to loss of uniformity, with weak spots along the cable length. Pretreatment of paper tape also makes the complete removal of impurities a questionable process.

The two high-voltage cable systems so far used to an extent that classifies them as standard are the ordinary "solid type" cable and the "oil filled" cable. The essential features of these two cable systems will be briefly outlined for purposes of leading up to a logical field of usefulness for the low-gas-pressure cable system that will be described.

Solid-Type Cable

This is the original type of high-voltage cable, used for 40 years or more. About 20 years ago a growing demand for higher operating voltages and reduced costs led to marked improvement in insulation quality and increase in working voltage stress (decrease in insulation thickness). Within recent years uniformity and practically perfect initial impregnation have been obtained by improvement of the impregnation treatment and the use of nonsolidifying impregnating compounds of lower viscosity.[1,2] Present-day insulation thicknesses represent an average working voltage stress of from 42 to 58 volts per mil.

Solid-type cable has now about reached its ultimate development. Present limitations are inherently physical. Complete impregnation has improved the insulation but has increased the physical problem. Today, we are faced with the following inherent difficulties:

(*a*). No method of controlling relatively wide pressure fluctuations due to temperature changes in service. Compound expansion during heating increases pressure and this puts an undue stress burden on the lead sheath, causing permanent stretching, fatigue splits, and other sources of leakage.

(*b*). During periods of cooling negative pressures (vacuum) develop. Air and moisture are drawn into the system through any leaks that exist.

(*c*). Stretching of sheath and migration of the nonsolidifying compound lead to void formation. During periods of negative pressure cumulative ionization damage is liable to occur.

Oil-Filled Cable

The success of oil-filled cable is too well known to require repetition here. It completely overcomes the inherent difficulties of solid-type cable just discussed.[3] Ionization is eliminated entirely and predetermined safe working stresses on the lead sheath are under control at all times. Positive oil pressure is maintained, preventing entrance of impurities at points of accidental leakage. Low working oil pressure (1 to 30 pounds, depending upon contour) greatly simplifies design, installation, and maintenance. This low pressure allows ordinary lead sheath finish up to 15 pounds and a simple reinforced sheath finish from 15 to 30 pounds. The cable can be installed anywhere that solid-type cable can, drawn into ducts, buried, etc.

Today, the average working voltage stress in oil-filled cable is from 80 to 143 volts per mil, depending upon voltage rating. This is more than twice that of solid type, and the ultimate reduction in insulation thickness has not yet been reached. It will depend upon required impulse strength to resist lightning and switching surges. Experience has shown that normal 60-cycle working stress is not a limiting factor in determining minimum insulation thickness. The rea-

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Paper number 39-71, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 1, 1938; made available for preprinting December 22, 1938.

G. B. Shanklin is engineer in the cable section of the central station department, General Electric Company, Schenectady, N. Y.

A number of engineers associated with the author, too numerous to list in full, have contributed to this development. The writer particularly wishes to acknowledge the valuable help of J. A. Scott and J. B. Felter, who carried out the laboratory tests, S. J. Garahan, W. C. Hayman, C. A. Piercy, E. L. Crandall, F. W. Engster, F. H. Buller, L. L. Phillips, V. A. Sheals, and T. C. Aitchison, all of whom gave generously of their support and encouragement. The fine spirit of co-operation shown by the Consolidated Edison and Yonkers Electric Light and Power companies is gratefully acknowledged.

1. For all numbered references, see list at end of paper.

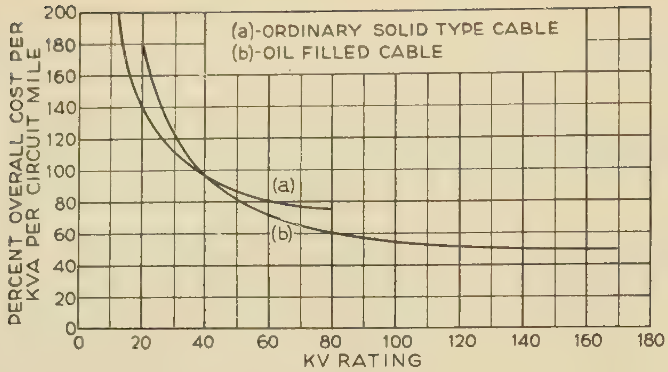


Figure 1. Unit over-all installed costs in underground ducts

son for this is that even one or two pounds positive oil pressure in the feed channels is sufficient to prevent void formation in a properly designed cable.[3] Accordingly, the oil-filled cable system is inherently a low-pressure system. There is no need of complicating matters by increasing oil pressure above that actually required by the contour and length of section to be fed. Technically, the standard low-pressure oil-filled cable system is correct and superior to all high-voltage cable systems so far devised. It is only from an economic standpoint that it leaves room for improvement.

Figure 1 gives comparative over-all installed unit costs for oil-filled and solid-type cable systems in underground ducts as a function of voltage rating. At any given voltage the costs will vary with load but the curves in figure 1 are representative averages for the most economical conductor sizes. It will be noted that the unit cost of oil filled increases more rapidly than that of solid cable systems with decrease in voltage rating. At 38 kv the costs are about the same. Below 38 kv solid-type cable is more economical in average cases.

The reason for this is that the reservoirs, stop joints, and other accessories used with oil-filled cable are relatively more costly than those used with solid cable. Total accessory cost decreases much more slowly than total cable cost with decrease in voltage rating, conductor sizes remaining the same. Also, total cable cost for oil filled, while considerably less at higher voltages, due to smaller diameter and lesser material, decreases more slowly than solid-cable cost as voltage rating decreases. Figure 1 explains why solid-type cable is still more commonly used below 38-kv rating.

Gas-Pressure Cable

Experience with oil-filled cable has demonstrated in a striking way the benefits of maintaining positive pressure during shipment, installation, and service. In attempting to develop newer and better cable systems these basic principles should be followed.

Maintenance of pressure requires a pressure medium. There are only two well recognized pressure mediums, liquid (oil) and gas. The development field utilizing oil as a pressure medium is now covered and is best exemplified by the standard low-pressure oil-filled cable system. The use of gas as a pressure medium leaves a more versatile field of exploration. In the way of simplicity, at least, it is attractive. There are no hydrostatic head pressures to deal with and the accessory designs are simple, at least for low pressure systems. Installation and maintenance are also simplified.

Soon after entering this field of development we concluded that a dry, inert gas in direct contact with and saturating the impregnated-paper insulation gave the best assurance of maintaining pressure through the whole cable cross section with minimum time lag during temperature and pressure variations. It was further concluded that longitudinal gas feed channels in the cable would facilitate control of uniform gas pressure along the cable length in service; in fact, were necessary fully to accomplish this purpose. The feed channels fulfill another important function in providing surplus gas at any point of accidental leakage. Without longitudinal feed channels that will readily convey surplus gas, pressure would be rapidly lost at such leakage points and cause failure.

The tests and development work leading up to present conclusions will be described in detail but first it will be explained why, at least for the time being, we are concentrating on low-gas-pressure systems of moderate voltage rating, and consider this the most promising field of development.

Figure 2 shows the voltage at which ionization starts as a function of gas pressure. The curve is a representative average of all the tests on single- and three-conductor gas-pressure cable that will be described, using nitrogen gas as a pressure medium. The measurements were made after load-cycle drainage of compound.

The voltage at which ionization started in these gas-filled voids is shown in figure 2. The drooping shape of the curve is significant and important. Fifteen pounds pressure comes within safe working limits for ordinary single lead sheath. The steepest part of the curve in figure 2, and a good part of the gain in ionization voltage, occurs in this relatively low pressure range. The ionization voltage at 15 pounds pressure is 80 volts per mil, which is the lower limit of working stress for oil-filled cable.

Forty pounds pressure comes within safe working limits for ordinary double reinforced sheath, as proved by some years of experience with oil-filled cable. This is the next steepest part of the curve and the ionization voltage at 40 pounds pressure is 112 volts per mil. This gain in voltage stress is not obtained, however, without increased cost. For average cable sizes double reinforced sheath increases cable cost from 10 to 15 per cent as compared with single sheath. This can be justified only when balanced by an equivalent saving in insulation cost (reduced thickness). Actually, it is not completely justified then, because years of experience with oil-filled cable has established the fact that single sheath at 15 pounds pressure has given a better service record than double reinforced sheath at 40 pounds pressure.

As a first stage in the practical development of gas pressure cable it is our intention to establish the ultimate safe operating voltage for cable with single lead sheath and 15 pounds pressure and resort to reinforced sheath with 30 to 40 pounds pressure only for higher voltages. At just what voltage this can be justified will depend upon field experience. We do not yet know ultimate safe working voltage stresses. At the present writing, and judging from laboratory tests alone, single-sheath cable at 10 to 15 pounds pressure will have an ultimate working voltage stress (average) of about 75 volts per mil, and reinforced sheath cable at 30 to 40 pounds pressure about 100 volts per mil. If this is proved by field experience then, economically, single-sheath cable will have a maximum operating voltage of about 40 kv. This voltage range happens to fill the economic gap caused by the relatively high cost of oil-filled cable below 38 kv as shown in figure 1. We believe that the greatest field of usefulness for gas-pressure cable is in filling this gap.

High-Gas-Pressure Cable

Referring again to figure 2, the ionization voltage at 200 pounds pressure is 162 volts per mil. This is by no means a sufficient gain in voltage stress, as compared with the 40-pound results, to warrant a fivefold increase in gas pressure, with the rather serious complications it introduces.

With few exceptions, important high-voltage cable lines in this country are installed in duct systems, mostly under paved city streets. Any cable used must be flexible so that it can be drawn from shipping reels into these ducts and trained around the manhole walls to form expansion bends and facilitate racking of joints. Gas-pressure cable with single sheath at 15 pounds and ordinary double-reinforced sheath at 40 pounds is readily adapted to these requirements. We are only concerned with the radial stresses on the sheath. Longitudinal stresses are of no great importance. Ordinary lead plumbing wipes are used and there is no tendency for the cable to move or "whip" due to excessive internal pressure. All of this has been demonstrated by experience with oil-filled cable.

At 200 pounds pressure a very elaborate type of sheath reinforcement would have to be used, taking care of both radial and longitudinal strains. All plumbing wipes would also have to be reinforced radially and longitudinally. Curvature in the duct run and expansion bends in the manholes would have to be dispensed with or the cable given rigid support with sufficient bearing surface to avoid crushing.

This special high-pressure reinforcement would add at least 20 to 25 per cent to cable cost as compared with ordinary single-sheath low-pressure oil-filled cable. This is, without question, more than the difference in cost of oil-filled cable accessories and high-pressure gas cable accessories.

On the above basis it is reasonable to conclude that the over-all installed cost of a 200-pound gas-pressure system for 138-kv service in underground ducts would exceed the cost of an equivalent low-pressure oil-filled system. In view of the experimental nature of reinforced sheath operation at 200 pounds pressure and the difficulties of maintenance and repair work, we feel it advisable first to determine the practical possibilities of low-pressure gas systems for moderate voltage use and leave the supervoltage field to oil-filled cable for a while longer.

Laboratory Tests

General Conditions

The series of laboratory tests on gas-pressure cable, started about five years ago, have included different types of impregnating compound, different densities of paper tape, various gases, and variations in method of constructing and treating the cable. Both single-conductor and three-conductor cable have been tested.

Single-Conductor Cable

All single-conductor cable used in these tests had either 350,000-circular-mil or 400,000-circular-mil stranded conductor with one-half-inch hollow core. The insulation was 0.300-inch impregnated paper tape, and over this a one-eighth-inch lead sheath. The cable was given a very complete vacuum drying and impregnation treatment and the test lengths (50 feet) were end sealed and sent to the laboratory without drainage, other than blowing the core free of compound and filling with gas to ten pounds pressure.

In the laboratory the lengths were drawn into 2.0-inch steel pipe and special high-pressure porcelain test terminals assembled at each end. The hollow core of the cable and the space in the steel pipe were interconnected through the terminals. Both were evacuated and filled with gas to required pressure. An insulated current transformer furnished load heating current to the conductor. The lengths were subjected to 60-degree-centigrade load cycles and simultaneous gas-pressure cycles from atmospheric to 100 and 200 pounds for a number of days until a capillary balance was reached and drainage of compound from the core ceased. Power factor versus voltage curves were then measured in pressure steps from atmospheric to 200 pounds at room temperature and 60 degrees centigrade. After this the lengths were subjected to load-cycle overvoltage endurance with periodic measurement of power factor and ionization. Two to three complete load cycles per day were applied from room temperature (25 to 30 degrees centigrade) to 60 and 65 degrees centigrade.

Three-Conductor Cable

The three-conductor test lengths were 75 feet long with 350,000-circular-mil sector conductors and 0.200-inch paper tape, shielded. All filler material was omitted from filler spaces, which acted as gas feed channels. The first test length had no support in the filler spaces. Remaining lengths had steel spiral support in the filler spaces similar to that used with oil-filled cable. The insulated and shielded conductors were cabled together with a metal-tape binder and a one-eighth-inch lead sheath was applied after treatment.

After vacuum drying and impregnation in the usual manner the lengths were left in the treating tank and drained of surplus compound in an atmosphere of the same gas later used as a pressure medium on test. Drainage was at 60 to 70 degrees centigrade for a considerable period of time. Afterward the lengths were leaded in an atmosphere of the same gas, end sealed, and sent to the laboratory under ten pounds pressure.

The lengths were subjected to short-time ionization tests up to 30 pounds pressure and long-time load-cycle overvoltage tests up to 15 pounds pressure. It was consequently not necessary to install them in steel pipe and all tests were made with bare lead sheath exposed. Ordinary porcelain pothead terminals were assembled in inverted position to facilitate drainage of compound from the cable, which, in effect, was arranged as an inverted U. The three conductors were connected in series for application of load-cycle current. In all other respects test procedure was as outlined for single-conductor cable, except that the load cycles were carried to 70 and 80 degrees centigrade.

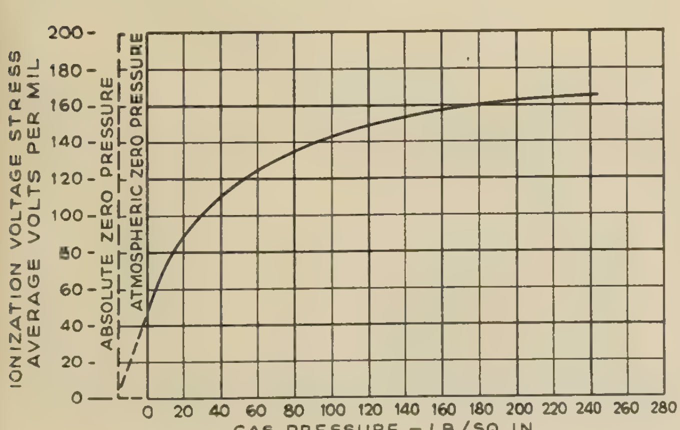


Figure 2. Voltage stress on solid insulation at which ionization starts as a function of gas pressure in voids of gas-pressure cable

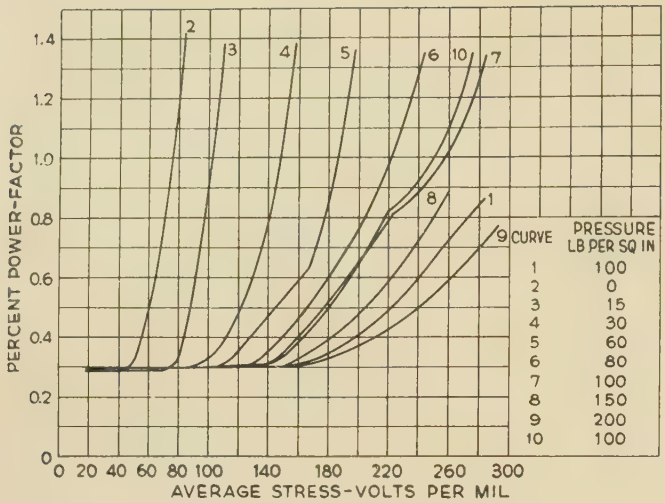


Figure 3. Test length number 5—ionization curves after prolonged load-cycle bleeding—measured at 25 degrees centigrade

Test Length Number 1. Single conductor, impregnated with relatively thin oil similar to that used in oil-filled cable and having a viscosity of 37 Saybolt at 100 degrees centigrade. Pressure medium, CO_2 gas.

Due to the thin oil and the large volumetric absorption of CO_2, the bleeding under load and pressure cycles was excessive, approximately 33 per cent of the original oil in the paper being expelled. Ionization was also poor, beginning at about 35 volts per mil atmospheric pressure. The length was submitted to a 65-degree-centigrade load-cycle endurance run at 300 volts per mil and 200 pounds pressure. Failure occurred in 22 hours.

Test Length Number 2. All conditions similar to those for length number 1 with the exception that the cable was impregnated with a heavier all-mineral-oil compound similar to that used in solid-type cable and having a viscosity of 95 Saybolt at 100 degrees centigrade

The expulsion of compound was improved, being about 25 per cent, and ionization was also improved. In spite of this, failure occurred in $7^1/_2$ hours on load-cycle endurance at 300 volts per mil, 200 pounds pressure.

Test Length Number 3. This length was a duplicate of length number 1, the only difference being that nitrogen gas was used instead of carbon dioxide. On load-cycle endurance at 300 volts per mil, 200 pounds pressure, failure occurred in 80 hours. This was an improvement compared with length number 1 but the thin compound still bled too freely.

Test Length Number 4. This length was similar to length number 2 with the exception that nitrogen gas was substituted for carbon dioxide. This gave decided improvement in both compound expulsion and ionization. On the same 65-degree-centigrade load-cycle endurance as length number 3 failure occurred after 801 hours.

Test Length Number 5. Exactly similar to length number 4 and all test conditions the same with the exception that the 60-degree-centigrade load-cycle endurance run was made at low pressure (15 and 10 pounds) and at voltage stresses just above the ionization point (80 and 100 volts per mil). The load-cycle endurance run was continued for a long period of time, more than two years.

Figure 3 gives 25-degree-centigrade ionization curves at different pressures after the length had been subjected to a large number of load cycles and alternate applications of vacuum and 200 pounds nitrogen pressure before the endurance run. Compound expulsion had practically ceased by the time figure 3 was obtained. The corresponding 60-degree-centigrade power factor curves are given in figure 4. It will be noted that ionization starts at a higher voltage at 60 degrees centigrade, as would be expected. It should also be noted that while the solid loss power factor at 25 degrees centigrade is practically horizontal before ionization starts the corresponding 60-degree-centigrade curves in figure 4 show a pronounced up slope of solid loss power factor as voltage increases. This is inherent in this type of cable and, since it obscures true ionization loss, only the room temperature results will be used as an indication of ionization in the load-cycle endurance charts that will be presented for the different test lengths. As a matter of fact, even the room-temperature measurements show this positive coefficient of solid loss power factor in most of the test lengths and this must be allowed for.

Figure 5 gives a chart of the 60-degree-centigrade load-cycle endurance run, 105 days at 15 pounds and 80 volts per mil, 255 days at 10 pounds and 80 volts per mil, and 470 days at 10 pounds and 100 volts per mil. The total length of the load-cycle voltage test was 830 days. At the end of that time the cable had stabilized so test was discontinued and the length carefully dissected for study of *X* wax.

Referring to figure 5, after the first 9 days at 15 pounds, 80 volts per mil, ionization practically disappeared and remained negligible until 100 volts per mil was applied at the end of 360 days. At the beginning ionization was more pronounced, as would be expected, but again decreased and practically disappeared after an additional 300 days. The 60-degree solid loss gradually increased from 1.0 per cent to approximately 2.0 per cent. It then decreased

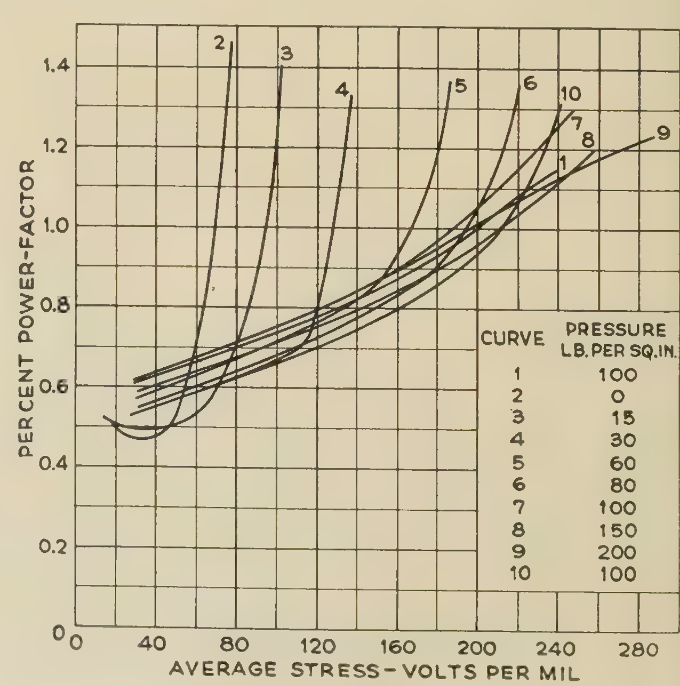


Figure 4. Test length number 5—ionization curves at same time and in same order as those in figure 3—measured at 60 degrees centigrade

and at the end of the run (830 days) had returned to the initial 1.0 per cent value. The "saw tooth" fluctuations in the power factor plots are typical of all tests made and are due to fluctuations in copper temperature. It is difficult to hold exactly uniform copper temperature during these measurements. It will be noted that the power factor and temperature fluctuations follow each other closely, particularly at 60 degrees centigrade where power factor is more sensitive to temperature changes.

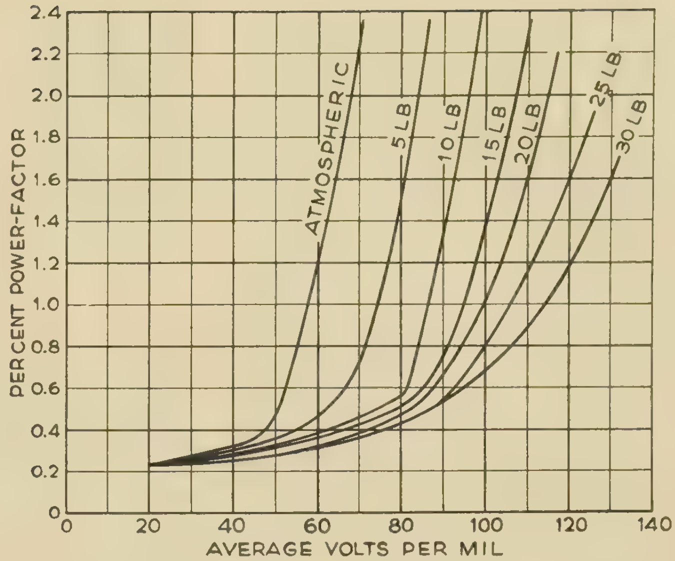
Wax distribution was uniform throughout the insulation thickness and occurred only in the butt spaces between paper tape edges. There were no signs of tree-design overstressing or cumulative ionization trouble, only the limited *X* wax formation in the butt spaces, previously described.

Test Length Number 6. Three-conductor cable, impregnated with same compound as length number 5 and also using nitrogen as a pressure medium.

Power factor curves (24 degrees) after stabilized load-cycle bleeding are given in figure 6. The 24-degree-centigrade solid loss power factor has a distinct upward slope with increased voltage stress. This not only obscures the starting point of ionization but must be allowed for in interpreting the room temperature ionization measurements on the load-cycle endurance run chart given in figure 7.

This chart shows that length number 6 was subjected to 80-degree-centigrade load-cycle endurance for 147 days at 75 volts per mil and 10 pounds pressure. Ionization was noticeable at the start but had practically disappeared at the end of 70 days (making due allowance for the upward slope of solid loss in figure 6). At the end of 147 days the cable was stabilized, so voltage stress was increased to 85 volts per mil. Again ionization appeared and gradually ceased after 49 days further testing. After that the cable was completely stable for a total time of 400 days. The same characteristic self-healing and self-extinguishing effect of ionization found with length number 5 is present in length number 6 after each endurance voltage step. The high-temperature solid loss also behaves in the same way, increasing some and then gradually decreasing to its initial value.

Figure 6. Test length number 6—ionization curves after load-cycle bleeding—measured at 24 degrees centigrade



After 400 days the test was discontinued and the cable subjected to vacuum treatment to remove all nitrogen gas. It was then filled with Freon gas to determine whether the high dielectric strength of this gas (more than twice that of nitrogen) would prove beneficial. Power-factor curves at different pressures showed that Freon increased ionization voltage approximately 50 per cent.

The 80-degree-centigrade load-cycle endurance run was then continued at 85 volts per mil, 10 pounds Freon gas pressure. Solid loss immediately began to increase and after 24 days the test was discontinued. At that time the 80-degree-centigrade power factor had increased to 7.0 per cent, more than double its original value. We are not sure of the real cause of this increase. It may have been due to chloride impurities in the Freon or it may have been caused by dissociation of Freon from ionization discharge. We intend to study the possibilities of Freon and other high-dielectric-strength gases further. In the meantime we will use nitrogen as the best of the available common gases.

The Freon test was stopped before the cable was seriously damaged and it was then dissected for study of *X* wax formation. Again wax was found only in the butt spaces, more or less uniformly distributed through the thickness, and without other signs of overstressing.

Test Length Number 7. This three-conductor length was exactly similar to length number 6 with the one exception that two layers of manila paper tape were bound over the metal shielding tape on each insulated conductor.

The power factor versus voltage curves at different pressures were similar to those in figure 6, but the upward slope of the solid loss power factor was more pronounced and calls for a greater allowance in the load-cycle endurance chart given in figure 8. Length number 7 was subjected to an 80-degree-centigrade ten-pound load-cycle endurance test at 85 volts per mil for 400 days. It will be noted in figure 8 that ionization during this time was more pronounced than in length number 6, figure 7, and required a longer time to heal itself. At the end of 300 days ionization had practically disappeared and the 80-degree-centigrade solid loss had returned to its initial value. Test voltage was then increased to 100 volts per mil and ionization became pronounced. Both ionization and solid loss showed a tendency to gradually decrease but a stable condition was not reached. At the end of 110 days at 100 volts per mil solid loss began to in-

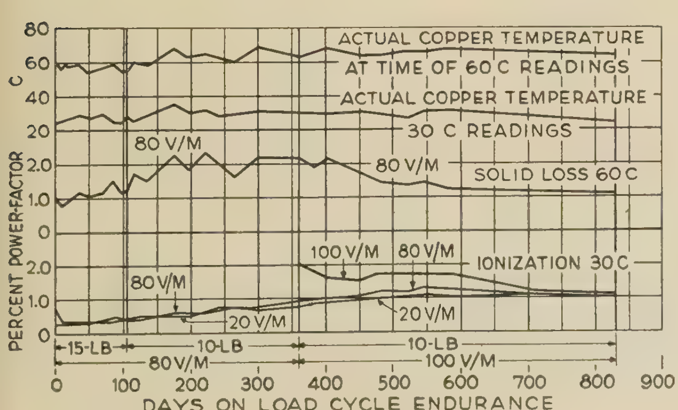


Figure 5. Test length number 5—load-cycle endurance at 80 and 100 volts per mil—15 and 10 pounds per square inch nitrogen pressure

crease. When the last measurements were made at 160 days the 80-degree-centigrade power factor had increased to 2.9 per cent. Failure occurred 7 days later (167 days, or 567 days total). No doubt there was a rapid increase in power factor during the last few days before failure.

Dissection of length number 7 showed the same distribution of wax in the butt spaces as found in length number 6. The wax was more pronounced, however, there were no carbonization, tree designs, or other signs of overstressing except at the point of failure.

Test Length Number 8. This length was impregnated with the same heavy mineral oil as lengths numbers 6 and 7, mixed with 1.5 per cent of an experimental, viscous compound to increase viscosity. The viscosity of the mixture was 350 Saybolt at 100 degrees centigrade. While the viscosity was increased there did not appear to be an equal increase in tackiness and film tension, the compound bleeding at high temperature being about the same as previously.

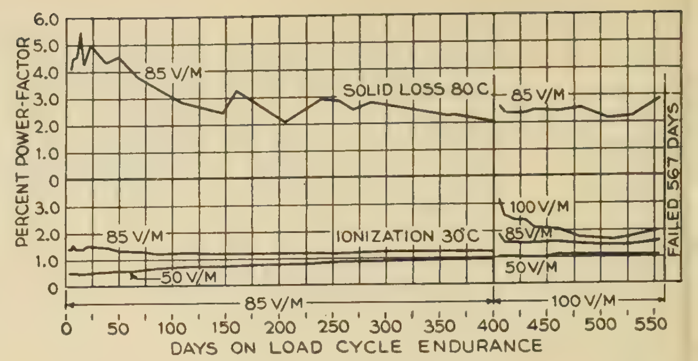
The 80-degree-centigrade ten-pound load-cycle endurance-run data at 85 volts per mil will be found in figure 9. The run has continued for 220 days to date and the length is still on test. As far as it has gone, the endurance run has shown about the same results as obtained with length number 6.

This length, alone, would not indicate any real benefit from increased viscosity of impregnating compound. We attribute this mostly to a lack of film tension in this particular mixture. Good film tension is, of course, necessary to hold the compound in place by capillary attraction.

Test Length Number 9. Similar to length number 8 except that the heavy oil compound contained 20 per cent rosin. The mixture had a viscosity of 200 Saybolt at 100 degrees centigrade. This mixture had more tackiness and better film tension than that in length number 8.

The rosin used was not the modern water-white type but was an older and less highly refined grade. It was desired to study the stability of this older grade before trying the newer.

Figure 8. Test length number 7—load-cycle endurance at 80 degrees centigrade, 85 and 100 volts per mil—ten pounds per square inch nitrogen pressure



The 80-degree-centigrade ten-pound load-cycle endurance chart at 85 volts per mil will be found in figure 10. Allowing for the upward slope of the solid loss power factor the ionization from the start has been quite small and seems to confirm the opinion that an impregnating compound of high viscosity and high film tension improves ionization characteristics in gas pressure cable. The solid loss, however, began to increase rapidly after 37 days and, to date (170 days), appears to be still increasing. Test will be continued on this length.

Hot spots were found along the cable soon after the increase in dielectric loss was observed but the cable has not yet shown signs of final failure. The instability of this length is undoubtedly due to the grade of rosin used. It is recognized as lacking the stability of highly refined, water-white rosin.

Test Lengths Numbers 10 and 11. These two lengths of three-conductor cable are similar to length number 9 with following exceptions. Length number 10 has 20 per cent highly refined water-white rosin mixed with heavy oil. The viscosity of the mixture is about the same as that for length number 9. Length number 11 is impregnated with an experimental hydrogenated oil having a viscosity of 1,000 Saybolt at 100 degrees centigrade.

These two lengths are now being made ready for load-cycle endurance. It will be necessary to report results at a later date. We are planning to include other test lengths using high viscosity compounds and fully determine the best viscosity for gas-pressure cable of this type.

Summary of Test Conclusions

The series of tests already described lead to the following observations and conclusions.

Paper Tape

It is important that the paper tape be applied smoothly and uniformly with the smallest possible space between butt edges and freedom from wrinkling. Impregnating compound is held in the insulation by capillary attraction. This means that the largest spaces (voids) are drained first, leaving only a film of compound on the walls. One main object is to keep such voids to minimum size since ionization voltage is a function of void size at all pressures.

The thinnest paper tape allowed by practical limitations (five to six mils) should be used. Tearing of tape and wrinkling are the chief limitations in this respect. The need of freedom from impurities goes without saying.

Paper density over the usual commercial range from 0.8 to 1.2 seems to affect characteristics only slightly. If anything, the lower density paper appears to give a little better results but this is not fully demonstrated.

Impregnating Compound

The primary requirements, such as high resistivity, oxidation stability, etc., are common to all types of impregnated paper cable. The gas-pressure cable puts particular emphasis on stability and minimum gas evolution in the presence of ionization discharge.

Film tension and viscosity are other important requirements. The compound should have tenacity and not bleed readily from the paper insulation. Also, it should leave a thick film on the walls of the larger voids, thereby reducing their size and offering a source of X wax for closing up these larger voids and extinguishing incipient ionization locally

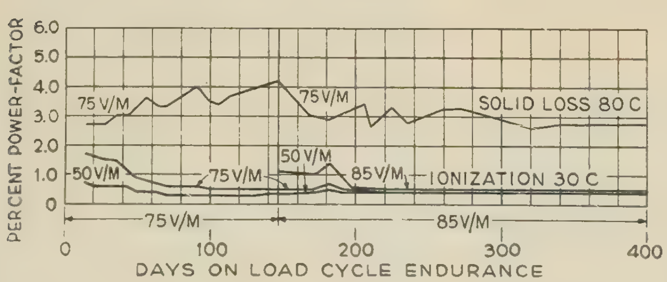


Figure 7. Test length number 6—load-cycle endurance at 80 degrees centigrade, 75 and 85 volts per mil—ten pounds per square inch nitrogen pressure

Table I. Insulation Thickness and Average Voltage Stress for Solid, Oil-Filled, and Low-Gas-Pressure Cable

Rating (Kilovolts)	Insulation Thickness (Mils)			Average Voltage Stress (Volts per Mil)		
	Solid	Gas	Oil-Filled	Solid	Gas	Oil-Filled
15	203	157	110	42.7	55.2	78.8
23	266	206	145	50.0	64.7	92.0
27	297	229	160	52.6	68.2	97.7
34.5	375	283	190	53.2	70.5	105.0
40	422	316	210	54.7	73.1	110.0

before it begins to spread and become cumulative.

The compound should not be of such high viscosity as to offer serious difficulties in obtaining uniform initial impregnation, or in draining the cable and gas channels sufficiently to prevent slug stoppages in service. Also, the compound should allow ready transmission of gas pressure through the insulation wall. Optimum compound requirements for gas-pressure cable are not yet fully determined.

Gas Pressure Medium

Of the readily available commercial gases it has been found that nitrogen is the most desirable. It has good dielectric strength, does not affect the solid insulation chemically, and its absorption by the impregnating compound is small. The present sources of supply specify a maximum oxygen content of 0.3 per cent. We are going to try to reduce this oxygen content. It has undoubtedly increased solid dielectric loss to a more or less extent in the tests that have been reported, by oxidation action during load-cycle heating. Other special gases of higher dielectric strength, similar to Freon are being given further study.

Ionization Stability

The development of low-gas-pressure cable was prompted by the observed behavior of ordinary solid-type cable when exposed to ionization discharge. There is a tendency toward cumulative ionization deterioration in solid-type cable. Observation shows this cumulative action takes place during periods of low temperature and negative pressure in void spaces. Maintenance of positive pressure, even if relatively low, will counteract this cumulative tendency markedly. A study of the low-gas-pressure cable bears this out. Initial ionization behaves in just the opposite way to that in solid-type cable. Instead of being cumulative in its action it is self-extinguishing and self-healing without permanent increase in solid loss.

This self-extinguishing effect is produced by formation of *X* wax which partially fills up the larger voids. Apparently, positive-pressure ionization, at voltage stresses below what might be termed a critical voltage, is not accompanied by the same harmful by-products that result from negative-pressure discharge, including gas evolution (mostly hydrogen) and conducting impurities (water and acids).

No doubt, the difference between ionization under negative and positive pressures is, in part, one of degree. With gas-pressure cable the size of voids and the pressure in these voids are under control. The critical voltage stress can accordingly be more safely approached. With solid type cable, however, neither the size nor pressure of voids is known or under control in all cases and there is always the possiblility of overstressing beyond the critical limit, which is not only lower than that of gas-pressure cable but variable and indeterminate.

Thermal Resistance

Measurements made after a year or more of 80-degree-centigrade load-cycle endurance give an increase in thermal resistance of the impregnated paper of from 10 to 15 per cent. This will have no practical effect on current-carrying capacity and it is proposed to give gas-pressure cable the same load rating as solid-type cable.

The possibility of using dry paper instead of impregnated paper was studied at the beginning and discarded for a number of reasons, one of these being the material increase in thermal resistance. The ready absorption of moisture upon exposure, the special precautions against this exposure, and the larger size of voids, offered other obstacles to the use of dry paper.

Critical Voltage Stress

It is proposed to operate low-gas-pressure cable at a minimum no-load pressure of ten pounds. The tests were accordingly made mostly at this pressure. Judging from these tests the critical average voltage stress is close to 100 volts per mil. We accordingly believe that a working voltage stress of no more than 75 volts per mil will give sufficient factor of safety, in view of the close control that can be obtained with cable of this type.

Economic Working Voltage Stress

Economically, the low-gas-pressure cable is very similar to solid-type cable. The accessories are equally simple and of practically the same size and cost. The gas channels, the drainage operations in the factory, and the various gas operations in both factory and field add a few per cent to cable cost. On the other hand, the reduced thickness of insulation and smaller diameter offer practically the same saving. Additional experience will be necessary in determining exact cost figures but, approximately, the curve for solid cable in figure 1 holds also for low-gas-pressure cable. On this basis, it can be seen that gas-pressure cable fits

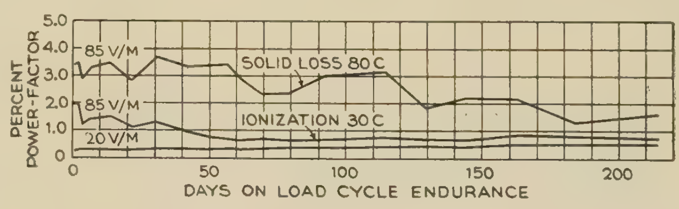


Figure 9. Test length number 8—load-cycle endurance at 80 degrees centigrade, 85 volts per mil—ten pounds per square inch nitrogen pressure

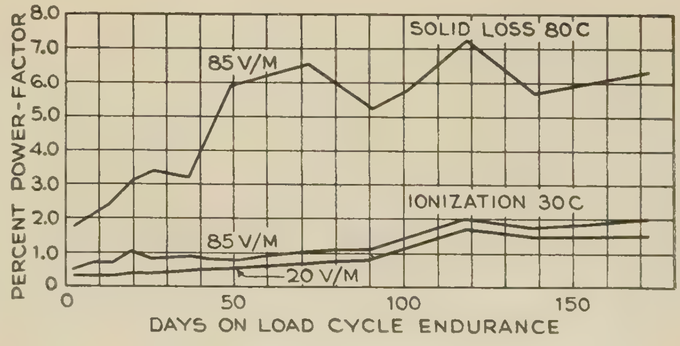


Figure 10. Test length number 9—load-cycle endurance at 80 degrees centigrade, 85 volts per mil—ten pounds per square inch nitrogen pressure

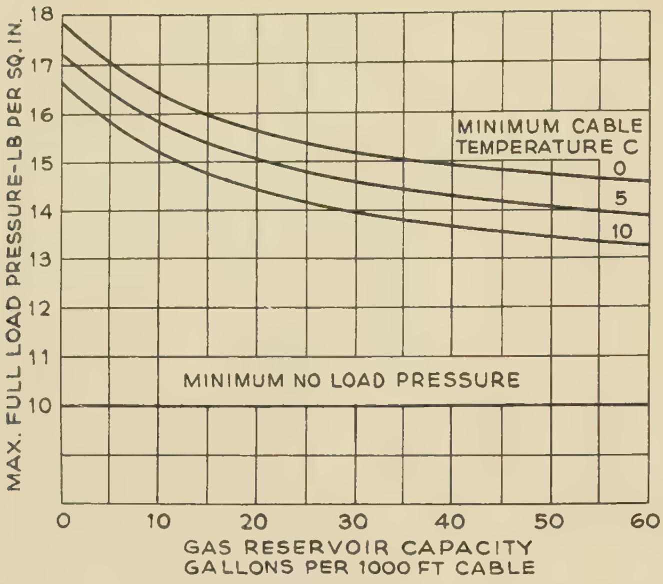


Figure 11. Fifteen-kv three-conductor gas-pressure cable system showing working pressure range as a function of auxiliary gas reservoir capacity, maximum copper temperature 81.5 degrees centigrade

into the economic picture best at 40 kv and below.

In determining insulation thickness for low-gas-pressure cable it is felt that a logical choice, as a starting point at least, is half way between the thicknesses for solid and oil-filled cable. Table I gives comparative thicknesses and average voltage stresses from 15 kv to 40 kv.

Field Installations

During the past summer (1938) the Consolidated Edison Company of New York and the Yonkers Electric Light and Power Company have ordered two trial installations of 15-kv low-gas-pressure cable. The first, consisting of a single three-conductor cable circuit 10,000 feet in length, was installed in Yonkers during August 1938, and is now in service. The second, consisting of two parallel circuits of three-conductor cable, each 5,000 feet in length, was installed a month later in the downtown district of New York City.

Identical cable was used in the two installations with 800,000-circular-mil sector-shaped conductors and 0.150-inch paper insulation. The insulated, shielded conductors were cabled together with steel spiral support channels in the three filler spaces. A single copper-bearing lead sheath 0.141 inch thick was applied in an atmosphere of nitrogen after the cable had been vacuum dried, impregnated, and drained of surplus compound. After leading the ends were sealed and the lengths shipped and pulled into ducts under ten pounds nitrogen pressure (average). The over-all diameter of this cable is 2.7 inches.

Both installations are paralleled in the same duct banks by solid-type cable of the same conductor size and voltage rating, also, the same current capacity rating and maximum copper temperature of 81.5 degrees centigrade. The Yonkers circuit has porcelain potheads at each end. The downtown circuits are spliced to solid-type cable at each end with stop joints at these points for segregation. No stop joints are used in either installation for sectionalizing, the gas feed channels having a free run from end to end.

These gas feed channels in each reel length were "blown out" with nitrogen gas before shipment and just before splicing in the field, to make sure no compound slugs were left. A few lengths showed a small amount of surplus compound in the feed channels. The majority of lengths were entirely free of slugs. The temperature and time of drainage before leading will be increased in the future until the correct values are learned by experience. It is expected that the need of blowing out slugs of compound from the gas feed channels, both in the factory and field, can be eliminated in this way.

When ready for splicing the cable ends were cut and the gas pressure reduced to two pounds. The exposed cable ends were plugged temporarily during the splicing operations with strips of sponge rubber, held in place and compressed with flexible band steel clamps. These temporary plugs were removed just before finally wiping the joint casing to the cable sheath and this last operation was done under gas flow. The total amount of gas lost during these operations was small after some experience was gained, amounting to less than 100 cubic feet of nitrogen per joint.

Because of the limited capacity of the filler-space feed channels and the desire to keep the gas pressure within a narrow working range from no load in the winter to full load in the summer, the Yonkers installation has galvanized steel tanks connected in the manholes to the cable joints at intervals of approximately 1,000 feet. These tanks are each of 53 gallons capacity and are, of course, filled with nitrogen gas to the same pressure as that in the cable. These tanks are simpler and more economical than the oil reservoirs frequently used with solid-type cable.

Figure 11 shows the pressure range of this 15-kv cable as a function of gas reservoir capacity. The curves are based on a minimum no-load winter pressure of ten pounds per square inch and show the maximum full-load summer pressure for different values of reservoir capacity. Three different winter ambient temperatures, zero, five, and ten degrees centigrade are assumed to illustrate their effects on cable pressure. With five-degree-centigrade winter temperature figure 11 shows the total working pressure range of the Yonkers installation to be from 10 pounds to 14 pounds.

For purposes of experiment no reservoirs were used in the downtown installation. Figure 11 gives a pressure range for this condition of seven pounds. To avoid stretching the lead sheath the maximum full load pressure is set at 15 pounds. This gives a total working pressure range from 8 to 15 pounds.

Accessories

The accessories used were much the same as those for solid cable of the same voltage rating.

Normal Joints

Similar to solid-cable joints with varnished-cloth tape reinforcement wrappings and metal shielding tape the full length of joint. The joints were filled with gas instead of compound and lead sleeve casings are seven inches diameter, offset toward the bottom to act as sumps for any surplus compound that might be drained from the gas channels. The casings are reinforced to withstand 15 pounds steady pressure.

In the future, on long lines where segregation for repair work is desired either stop joints will be used at intervals or semistops will be incorporated in some of the normal joints.

Stop Joints

Of the Herkolite core type used also with solid and oil-filled cable. The de-

sign has been described in a previous paper,[4] the only difference being that gas filling was substituted for oil filling and the cork-gasket semistops at each end omitted.

Potheads

Standard three-conductor porcelain potheads were used. A special cork-gasket semistop was incorporated in the wiping bell and the pothead above this semistop filled with petrolatum. The gas pressure below the semistop maintains the whole pothead under positive pressure.

Gas-filled potheads have worked successfully on laboratory and factory tests and will probably be used in the future.

Gas-Pressure Relays

Each installation has gas-pressure relays at the terminal ends, set to alarm at pressures a little above and below the normal working range. These pressure relays are of the bellows type similar in principle to those used with oil-filled cable but smaller and of lesser cost.

Profile

The profile of these first two installations is reasonably flat but, obviously, profile is of no consequence with gas-pressure cable. With solid and oil-filled cable profile must be taken fully into account on steep grades and at vertical risers. With gas-filled cable any height of vertical riser within the limits of mechanical strength can be safely used.

The gas-filled cable would appear to solve a real problem for use as risers in tall buildings and in localities where steep hills are encountered.

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Discussion

L. I. Komives (The Detroit Edison Company, Detroit, Mich.): We are glad to see a type of cable which in our estimation is designed primarily to relieve the lead sheath. It may seem that this aim is accomplished at the expense of the quality of the insulation; however, this may turn out to be merely an assumption. In any case, the ratio of the factor of safety of the insulation and of the lead sheath will be brought closer together by the means outlined by the author.

It seems that the remark in the paper that "profile is of no consequence with gas-pressure cable" would have to be proved in field installation. If this statement will be proved by successful field installations, we are looking forward to the use of this new type of cable for vertical risers.

In calculating costs, it should be borne in mind that most manholes are made for joints of more or less standard sizes. The author mentions joints seven inches in diameter with an offset toward the bottom which will certainly increase the size. Such large joints may necessitate the enlarging of present manholes.

When all accessories are taken into consideration, the low-gas-pressure cable differs from the oil-filled cable only in the use of accessories for gas supply instead of oil supply. From previous experience, we know that the oil-filled cable is a rather expensive installation. For this reason, until cost data are available, we seriously doubt the economy of this type of cable as against the standard solid-type cable for voltages below the 38 kv as mentioned by the author.

Robert J. Wiseman (The Okonite-Callender Cable Company, Passaic, N. J.): I have read with great interest Mr. Shanklin's paper as he describes the trials, tribulations, and hopes of an investigator of a cable design which is not too well known in commercial practice.

Cables in which gas is used as a means for maintaining the quality of the insulation are not new and we find many patents describing specific designs. Almost all of them pertain to the use of gas under high pressure. For some unknown reason, mostly everyone wants to operate the cable around 200 pounds per square inch internal pressure, yet it is possible to get commercially very good stability as low as 75 pounds per square inch pressure and we do not have the problems of leakage that exist at high pressures.

I am not so sure that solid-type cables have about reached their ultimate development. As I view it, we can improve solid-type cables if we loosen up slightly on the tightness of our sheaths and design our joints to permit free flow of oil between the joint and cable. This means the elimination of the varnished-cambric wrapped joint and the use of tubes as was done years ago with a fluid oil in the joint instead of hard or solid compounds. Tests we have conducted in our laboratories have shown an increase in life of a cable. A simple solution to the problem, yet it is to be regretted that we are wedded too much through custom to the use of tightly wrapped and solidly filled compound joints. Granted that the joint may not have as high a breakdown voltage as a tightly wrapped joint, it is more than ample for operation and at the same time, we reduce the pressures which a cable will be subject to and also prevent the cable dropping to a high vacuum when cooling.

It is true that the low-pressure oil-filled cable has contributed to the success of high-voltage cables and it is a question of economics whether it should be used or solid-type cable. The high-oil-pressure cables are equally able to meet the needs of high-voltage operation and are also dependent on the economics of the installation. The jointing is not as difficult as for the low-pressure oil-filled cable as the oil is of a higher viscosity and will not bleed from the cable during the making of a joint. It is true that the reinforcing is more expensive, but with the higher voltage breakdown which is obtained on the high pressure, it is a question of what factor of safety is desired and the price one wishes to pay for it.

The high-gas-pressure cable has been proved successful in Europe. We have the Beaver design which is the same design as Mr. Shanklin gets by drainage, as Beaver uses preimpregnated paper and his method of manufacture eliminates the filling in of the paper gaps and between layers with oil. Beaver uses 200 pounds per square inch N_2 gas pressure. The Hochstadter gas-pressure cable has a lead sheath between the insulation and the gas and the latter does not function as a part of the insulation. The Callender's Cable and Construction Company have two designs of high-gas-pressure cable, one uses impregnated paper and over the paper is wrapped varnished silk tapes before the shielding tape is applied. A loose lead sheath permits the filling of the space between the cable and the sheath with N_2 gas at 200 pounds per square inch pressure. The varnished silk tapes prevent the oil from draining from the cable and also the absorption of gas by the oil, thereby maintaining stability of the insulation. This type of cable has been under successful test for two years at Arnhem, Holland, and is the only design of high-gas-pressure cable with impregnated paper that has been successful. It has only been equalled by a low-pressure oil-filled cable which uses a heavier wall of insulation and a lower maximum stress. In addition, Callender's have been testing a similar type of impregnated gas-pressure cable in their own research laboratory for several years, first at 200 kv with the same current required at Arnhem, later they raised the voltage to 300 kv and to date the same cable has withstood 65 load cycles at 85 degrees centigrade on the copper. We also have made and tested an impregnated gas-pressure cable in our laboratories and have proved in its stability with tests on a 500,000-circular-mil round conductor, 0.450-inch wall, starting at 95 kv (312 volts per mil maximum electric stress) for 1,482 hours and 133 kv (438 volts per mil maximum electric stress) for 549 hours. The cable did not show any ionization between 35 volts per mil and 170 volts per mil both hot and cold for the last 20 days of test. The power factor was about 0.48 per cent at 70 degrees centigrade and 0.53 per cent at room temperature for the last 20 days of test.

I believe the reason for the failure of the cable which Mr. Shanklin tested at 200 pounds pressure was due to the absorption of gas into the oil with the resulting lowering of the dielectric strength and the increase in power factor. During our studies eight years ago on our Oilostatic cable system we used gas as the pressure medium and a gas cylinder was connected directly

to the potheads at 200 pounds pressure. Every few months we noted an increase in the power factor of the pothead which required draining of oil and refilling. The drained oil was milky in appearance and had about 20 per cent gas content. We introduced an intermediate oil reservoir with a diaphragm between the gas cylinder and the pothead which eliminated the trouble. The important thing is to keep the gas from contact with the oil that is under stress. The success of the Callender's impregnated gas-pressure cable comes from the varnished silk between the insulation and the gas.

Mr. Shanklin is using for the low-gas-pressure cable, comparatively low electric stresses but it is the maximum stress at the conductor that is important. According to figure 2 at 15 pounds per square inch gas pressure, ionization starts at 70 volts per mil and to be safe, one should take not over 80 per cent of this or 55 volts per mil. This does not agree with the statement under "Critical Voltage Stress" in which Mr. Shanklin proposes a working stress of 75 volts per mil. In other words, it is necessary to increase his wall thickness. Although the mixing of gas and oil is not as serious as if high gas pressure were used, still with time as expressed in years it seems as if there will be sufficient gas absorption by the oil seriously to reduce the breakdown voltage of the cable. Perhaps today we are not as concerned about getting high-voltage breakdown of our cables as we did formerly, in which case the cable manufacturer can take advantage of it as suggested above for solid-type cables. The need of as low gas absorption as possible is shown by the different results which Mr. Shanklin obtained with CO_2 gas and N_2 gas. The former is easily absorbed into oil, the latter with great difficulty but also hard to remove once it is absorbed.

Mr. Shanklin very nicely describes the problems in manufacture of a low-gas-pressure cable where the oil is permitted to drain. The size of voids is the controlling factor in stability and, therefore, careful taping is essential.

I am surprised to note that the thermal resistance is only 10 to 15 per cent higher than solid cables. I would expect it to be much higher. Tests on insulation subject to high gas pressure show a value of about 660 thermal ohms per centimeter cubed. Perhaps Mr. Shanklin has in mind as a reference value the 700 thermal ohms per centimeter cubed which we now use for paper cables and, therefore, actually about 800 thermal ohms per centimeter cubed. New solid cable shows as low as 450 thermal ohms per centimeter cubed but due to oil migration and drainage, with time, the 700 thermal ohms value is used in calculations. His curves of load-cycle endurance indicate a higher power factor than we are getting for solid cables and, therefore, it is questionable that the same current-carrying capacity can be used as for solid cables when we also consider the higher thermal resistance.

I am glad to note that this cable design is to be limited for the present to low voltages as it is essential that operating experience be gained to find all the sources of weakness that may be present, such as porosity of joint wipes and the ultimate absorption of gas to the saturation point.

I wish to congratulate Mr. Shanklin on a most interesting and well-prepared paper on another cable design which may have its usefulness provided we are willing to accept a lowering of some of our past requirements, namely, high-voltage breakdown, low power factors, and not too high thermal resistance.

E. W. Davis (Simplex Wire and Cable Company, Cambridge, Mass.): Mr. Shanklin's paper brings out the details of a very interesting new development in cable using a semi-impregnated paper cable supplied with moderate gas pressure to increase reliability by compensation for expansion and changes in the insulation due to load cycles.

The new design is one of the many new trends in cable progress which employ, more and more, various pressure devices to improve cable operation, increase allowable stresses, and remove some objectionable characteristics developing as a result of service practice.

It promises well but being intermediate between oil-filled and gas-filled cables must possess some good features of each and also some objectionable ones. Oil-filled cables under pressure have high impulse strength to transient voltages and since the insulation is voidless no damage should be present from overvoltage up to failure point. Gas-filled cables on the other hand have somewhat lower impulse strength but overvoltages usually produce self-healing faults.

The low-gas-pressure cable is midway between these and provides a composite dielectric of three components, oil, paper, and gas. Transient voltages sufficient to ionize gas-filled voids would not necessarily be self-healing and might produce permanent damage which would later develop into trouble. We are wondering if impulse tests have been made on these cables and faults obtained examined or if high-voltage life tests have shown the effect. The paper, under "Impregnating Compound," indicates that the weakness has probably been considered by the author. We note that long-time ionization is said to make self-healing voids by the formation of X. This takes time and would not take place with high transient voltages.

W. F. Davidson (Consolidated Edison Company of New York, Inc., New York, N. Y.): Mr. Shanklin has presented an interesting discussion of a new type of cable which we are glad to welcome in the American market. It is encouraging to see progress being made in the direction of new types of cable because it seems that this offers one of the most encouraging means for reducing the over-all cost of cable systems.

My first specific comment is to quarrel with the use of the term "negative pressure." When we are dealing with gases or liquids, such a term is quite meaningless in spite of its use in technology and it has the serious objection of possibly leading us astray when we come to analyze the underlying phenomena.

In his discussion under the heading "Ionization Stability" the author suggests that there is a "difference between ionization for negative and positive pressures." For any fixed electrical stress, there is a difference in the intensity of ionization but I am unfamiliar with any theory which will account for a difference in the action. Is ionization of any degree objectionable and if so, what determines the border line? The author suggests that there is something more than a difference in degree and it would be of great interest to all of us to know something more as to the details.

L. J. Berberich (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The results given by the author on the low-gas-pressure cable indicate a better performance under load-cycle test than one would expect. Gas pockets exist and ionization admittedly occurs just as in the solid cable with the one important difference; namely, a positive pressure is maintained in the gas pockets at all times. This naturally raises the critical voltage at which ionization begins. The success of the cable, however, appears to be in the discovery made by the author and his associates that ionization as measured by power factor change with voltage becomes negligible after the cable has been subjected to load-cycle test for a time.

This diminution of ionization with time on load-cycle test is ascribed to a self-healing process in which the area surrounding the gas pockets is sealed with the solid polymerization product formed as a result of the discharge. If this is true, an oil that produces large quantities of wax on subjection to corona discharge may be desirable. In this connection, I would like to ask if the addition of some aromatic compound, such as diphenyl, to the impregnating oil has been tried. In my work along these lines (*Industrial and Engineering Chemistry*, volume 30, 1938, page 280), it was shown that compounds of the aromatic type decrease the gas evolution from oils markedly and may increase the wax formation; the latter, however, could be observed only in a qualitative way. It appears possible that such additions to the impregnating oil may have application in this type of cable.

The authors report that when the high-dielectric-strength gas Freon was introduced into the cable, a very short life was obtained. It is questionable whether any of the high dielectric gases will be satisfactory as pressure mediums in this cable. Most of these gases are hydrocarbons which are either chlorinated or fluorinated or both. Compounds of this type are known to split off halogens under conditions of corona discharge. Since oil is also present which is an abundant source of reactive hydrogen under these conditions, the formation of strong acids is very likely. Some of these gases have hydrogen in the molecule which may provide another source for hydrogen.

Victor Siegfried (Worcester Polytechnic Institute, Worcester, Mass.): Mr. Shanklin has described an interesting solution for some of the problems involved in paper insulation of high-voltage cables, where the ionization of gaseous voids seems to be the limiting factor in the voltage rating of the cable. The philosophy of his method seems to reverse that of the cable using oil under pressure, and appears to be that if the inevitable voids in oil-filled cable establish

the working limit of voltage, let us then design the cable to have only voids, but control the effect of the voids by definite regulation of the pressure of the gas in them. This method of attack has led therefore to the use of gas at relatively low pressure and simple mechanical features, beyond which pressure, the economics and other factors involved indicate diminishing returns for any further increase in pressure.

There is one point in regard to which the language should be kept clear, however. This cable uses paper insulation, with gas under pressure as the saturating medium. I would prefer to classify this cable as a *gas-filled cable*, leaving the *pressure cable* designation open to future developments of cables at much higher gas pressures. The conclusions which the author presents indicate an optimum pressure of about 40 pounds per square inch pressure for this type of cable, whereas for real high-voltage cables using gas as the major dielectric, higher pressures appear to be economically desirable to realize the full benefit of the gaseous insulation properties. Mr. Shanklin's work is valuable in its field, but there is much room for development and research along the lines of a real gas-pressure cable using the higher optimum of pressure.

Wm. A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): This paper is probably destined to be one of the classics of cable engineering as it is one of a very few that have appeared announcing the development of an entirely new type of cable.

Like most new inventions many close approaches have been made to it but, for one reason or another, they did not, so to speak, "go over the top." There is nothing new in a drained cable or in gas pressure or even in the combination of a drained cable with internal gas pressure. The invention, as I see, lay in the realization that a limited gas pressure would give to a drained cable a sufficiently high ionization point to permit a cable of economical dimensions to be designed for the voltages at which three-conductor solid-type cables are used. Hitherto, workers with gas pressure cable had concentrated their efforts on the higher voltage field already occupied by the oil-filled cable.

Our laboratory has worked on a drained paper cable, with interstitial gas at atmospheric pressure. With this cable, the rise of the ionization point with continued application of voltage was as definite as in Mr. Shanklin's cable. Ionization started at a maximum stress of 50 volts per mil, calculated by the ordinary logarithmic formula. The stress was then raised to 60 volts per mil at which the dielectric loss was eight times as great as that at 50 volts per mil, and continued for three months at room temperature. During this period the ionization stopped. The voltage was then raised until ionization started, and it was found that the critical stress had risen to 65 volts per mil. I do not believe that ionization ceased at 60 volts per mil, as the result of filling the interstices with cable wax, as suggested by Mr. Shanklin, as very little wax was found.

Furthermore, a nitrogen-gas-filled cable at 30 pounds per square inch absolute pressure tested for 40 heat cycles at *zero voltage* showed a steadily decreasing ionization. Whereas for the first 10 cycles the power factor at 100 volts per mil ran over three times that at 60 volts per mil (average stresses), after 40 heat cycles, there was little difference between the power factors at the two stresses, that at the lower stress having remained constant. All I can say about the cause of this suppression of ionization is "I don't know." It would make a nice subject for a university thesis.

The ionization point of drained cable was also raised by the use of carbon-compound gases instead of air or nitrogen but these introduced difficulties peculiar to the gases employed.

The maintenance of pressure by the low-pressure-gas cable depends on the integrity of joint wipes and particular care will be required in testing for leaks. It is possible that a test similar to the soap bubble test will be useful, using instead of soapy water, such a material as Nekal, a product of I. G. Farbenfabrik, which is claimed to be much more efficacious than soap solution.

The impulse transient dielectric strength of drained cable is only about 15 per cent less than that of well-impregnated cable, so that on that basis, insulation walls would not have to be more than 18 per cent greater than those of oil-filled cables, which are based on impulse strength. It would therefore seem that Mr. Shanklin's table of thicknesses is conservative.

There are, of course, features of uncertainty in this cable which only time will clear. One of these is whether the eventual drainage of oil from the paper will not unduly lower the dielectric strength of the insulation. Another is the effect on the lead of long continuous application of the moderate pressure used. Laboratory tests indicate that while the stretch of lead is slow at such pressures, it is nevertheless definite and certain and that it occurs at an accelerating pace, as the lead becomes thinner. In view of such uncertainties inherent in a pioneer installation, the Consolidated Edison Company deserves the thanks of the industry for taking the initiative.

Joseph Sticher (The Detroit Edison Company, Detroit, Mich.): The paper by Mr. Shanklin brings out an apparently beneficial action of wax in cables. The beneficial action of wax referred to here consists of the closing up of larger voids and the extinguishing of ionization in these voids before it begins to spread and become cumulative. Until recently, cable wax was looked upon as visible proof that deterioration was occurring in the cable insulation and that this would eventually lead to breakdown of the cable. Wax was therefore abhorred and efforts were made to develop cable impregnants which would produce a minimum of wax when subjected to corona discharge.

During the past decade and more The Detroit Edison Company has studied the effects of corona discharge on oils and oil-impregnated papers with the view of determining changes in the electrical, chemical, and physical characteristics. From the results of this study, some of us gained a new viewpoint regarding cable wax, as follows:

It appeared that possibly wax should not be abhorred but really welcomed since it might be looked upon as a sort of natural self-defense of the cable against the ravages of ionization occurring within it. This conjecture was expressed in a paper presented before the National Research Council in November 1938. ("The Effect of Corona Discharge on Cable Insulation," Joseph Sticher, D. E. F. Thomas, C. D. Robb, and F. M. Hull; presented at the Conference on Electrical Insulation of the Division of Engineering and Industrial Research of the National Research Council, November 3-4, 1938, at Pittsburgh, Pa.)

The results which Mr. Shanklin relates in connection with tests on various test lengths of cable are very interesting in that they show that ionization under a given set of conditions gradually disappeared, to reappear only after an increase of voltage. These results are in support of the suggested hypothesis of self-defense of the cable in the following manner:

The gradual disappearance of ionization seemed to be due to formation of wax in voids. This wax tended to decrease the size of the voids with subsequent load cycles until ionization could not be maintained any longer at the prevailing voltage gradient.

When this apparently beneficial action of cable wax is considered still further, it appears that it might have potential possibilities to aid in the constant efforts to improve solid-type oil-impegnated paper cables somewhat as follows:

If a cable impregnating compound were found or developed which would form wax very rapidly under ionization, voids in a cable impregnated with this compound would rapidly be filled with wax to the extent that ionization would be extinguished. A certain amount of increase in total void volume would result from this waxing because of the increase in the density of the compound during the polymerization or condensation. The voids making up the total void volume would, however, then consist of two classes: first, voids too small to be ionized even in the region of highest voltage gradient; second, all voids including maximum size located in regions of voltage gradient too low for ionization to occur. In this connection it should be pointed out that compounds which form wax readily under corona discharge, such as unsaturated and aromatic oils and certain of the naturally occurring sulfur compounds, are at the same time compounds which produce only a little gas under this treatment. This coincidence is of decided advantage in the present application. In the unsaturated class, olefins such as hexadecene and higher homologs; or turpenes such as pinene or higher homologs, or polymerized products of turpentine; or in the aromatic class, polyindene or homologs of xylene; or in the sulfur class, the sulfides, might be desirable. It must also be realized that the wax, to be of use in this cable, should not be soluble in the impregnant and should not carbonize in the time it takes for ionization to be extinguished.

Whether a cable could save itself in this manner would depend essentially on what might be called the "waxing ability" of the impregnating compound. If this waxing ability is low, the insulation might fail before sufficient wax could be formed adequately to reduce the size of the affected voids.

Efforts "to fill the economic gap caused by the relatively high cost of oil-filled cable below 38 kv" would be best served, it seems, by improvements of the solid-type cable, which would allow this type of cable to remain self-contained. The *H*-type cable is an example of such an effort and that improvement has gone a long way toward solution of the problem. In other improvements of the solid-type cable a considerable amount of auxiliary equipment is usually entailed. The low-gas-pressure cable appears to come close to the "self-contained" solid-type cable. If, however, a freely waxing cable impregnant, used in place of the present impregnants, is developed to extinguish ionization as indicated, then a truly "self-contained" solid-type cable of

satisfactory performance may possibly result.

G. B. Shanklin: Mr. Del Mar's kind and encouraging remarks about this development are very much appreciated. His comments regarding the self-healing effect of ionization on a test length of drained solid-type cable are quite interesting. Mr. Del Mar states that he obtained this self-healing effect without pronounced *X* wax formation which invariably accompanies the same effect in the low-gas-pressure cable. I think the difference is explained by the physical construction of the two cables. Mr. Del Mar's cable did not have longitudinal feed channels for maintaining uniform gas pressure. The pressure in voids accordingly must have varied over a wide range with load cycles, and ionization gradually improved as the cable became gas-saturated with a gradual increase in pressure.

I readily admit that formation of *X* wax may not be the entire answer to this improvement in ionization in the gas-pressure cable. Perhaps further investigation will bring out additional factors. Our work shows however that formation of wax is without doubt one of the major factors in this self-healing effect. To give the best results this wax formation should be accompanied by a minimum amount of by-products, such as gas evolution, water, and other conducting impurities.

In regard to the possibility of porous wipes and other sources of leakage, it might be pointed out that the gas-pressure cable system is self-maintaining and so far there has been no difficulty in locating and repairing leaks that in an ordinary solid-type cable system would not be discovered until service failure occurred. The two field installations described in the paper are now completely gas tight, and no real difficulty was experienced in obtaining these results.

Mr. Del Mar has analyzed the transient impulse strength characteristics of the gas-pressure cable very accurately. This cable has an impulse strength midway between that of oil-filled cable and solid-type cable. We are in no way worried about the sufficiency of impulse strength in service.

Referring to the question raised by Mr. Del Mar regarding the possibility of continuous drainage of compound in service, we have subjected lengths of this cable to severe load cycles over a period of two years or more and have found in every case that drainage of compound from the paper insulation stabilized in a relatively short time and after a balance was reached between gravity and capillary attraction. The total drainage before this balance is reached is a function of compound viscosity and film tension (tackiness).

Mr. Del Mar also raises the question of gradual and continuous sheath creepage in service. Fortunately we have had a good deal of experience with oil-filled cable and sheath creepage has also been exhaustively studied in the laboratory. This work has shown that when the stress on the lead sheath does not exceed 125 pounds per square inch of sheath no appreciable creepage takes place.

Messrs. Berberich and Sticher both have a very accurate and parallel conception of the low-gas-pressure cable. The principle upon which this cable is based is in many ways opposite that generally accepted for solid-type cable. In the past, as Mr. Sticher states, ionization and wax formation in a cable have been abhorred. I think the chief reason for this is that wax formation in solid-type cable is usually accompanied by wide pressure fluctuations and undesirable by-products, such as gas evolution, water, and other conducting impurities. The wax itself has not been the real culprit.

In the gas-pressure cable the goal aimed at is uniform pressure maintenance and maximum wax formation with minimum gas evolution and by-products. The test results given in the paper show that this goal has now been closely approached. We are actively continuing our study of the most favorable compound characteristics, and we hope in time to obtain further improvements in this direction. We intend to continue our present trials of all possible compound compositions. So far we have found that the compound requirements for this gas-pressure cable are not greatly different from those for ordinary solid-type cable as regards stability and long life.

Referring to Mr. Davidson's discussion, I plead guilty of loosely using the term "negative pressure." It is more an expression of convenience than scientific accuracy and is used for the purpose of avoiding confusion in referring alternatively to absolute zero pressure and atmospheric zero pressure.

The last question raised by Mr. Davidson can only be answered by a good deal of additional research. I can only say that our work seems to indicate that there is more than merely a difference of degree in the effects of ionization at different pressures. Ionization in a partial vacuum seems to lead to more harmful gas evolution and more conducting by-products than corresponding discharges at higher pressures. Whether this is a question of differences in mechanism, chemistry, or both, I do not know.

Replying to Mr. Komives, it is not our purpose intentionally to lower the insulation quality in gas-pressure cable for purposes of obtaining better sheath performance. Actually we hope to obtain both better insulation quality and better sheath performance as compared with solid-type cable. We believe that the tests described in the paper bear this out. The gas-pressure cable remained stable under severe load-cycle heating at an average stress of 85 to 100 volts per mil. If stability under these conditions is accepted as a criterion of quality, we do not believe that ordinary solid-type cable could show any improvement.

Mr. Komives raises a question regarding the relative size of the joint casings for gas-pressure cable. It is true that the joint casing diameter is larger for purposes of acting as a sump. The length of the joint however is exactly the same as that for solid-type cable of the same voltage rating. It is length and not diameter of joints that is affected by manhole dimensions.

E. W. Davis asks about the impulse strength of this gas-pressure cable. I believe this question has already been answered in Mr. Del Mar's discussion. We believe the impulse strength of this cable to be somewhere between that of corresponding oil-filled and solid-type cable.

Doctor Wiseman seems to feel that solid-type cable can be improved by providing more ready means of longitudinal flow of compound. He suggests that a loose-fitting lead sheath and joints without reinforcement wrappings will help in this respect. This happens to be a possibility we have given a good deal of study to in the past. The whole trouble is that these changes will introduce all of the difficulties of oil-filled cable without the compensating benefits. As with oil-filled cable, it would be necessary to accept cumulative hydrostatic head pressure and control this with stop joints, reservoirs, etc. The benefit of this complication would not be obtained however, since it would be impossible also to control and maintain positive pressure over the whole cable length during load cycles. This can only be done with a relatively thin oil and sufficiently large longitudinal feed channels, in other words, an oil-filled cable. There is nothing in between as far as oil feed is concerned.

I cannot see any real benefit in the type of high-gas-pressure cable described by Doctor Wiseman, having wrapped varnished silk tapes over the cable insulation which act as a semi-impervious membrane. It seems to me that these silk tapings would merely retard but not prevent gradual gas saturation of the impregnated paper insulation. They would also interfere with initial drying and impregnation treatment in the factory and maintenance of uniform pressure throughout the cable cross section in service. It was for these reasons that we abandoned the idea of semi-impervious wrappings in the low-gas-pressure cable, where, at best, pressure is at a premium and there is none left to waste.

Doctor Wiseman seems to have missed the point that the low-gas-pressure cable is gas-saturated before it is shipped from the factory. This is accomplished as part of the regular factory treatment, and the reel lengths of cable are shipped under gas pressure. He need have no worry, then, about the effects of gradual gas absorption during service.

Reverse-Rotation Test for the Determination of Stray Load Loss in Induction Machines

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VARIOUS methods for the accurate determination of stray load loss in induction machines have been devised in recent years. Also older methods have been improved and developed as the nature of the loss has become better understood. The employment of different methods of testing on a given machine seldom shows complete agreement in results, due to minor discrepancies and inaccuracies inherent in the methods themselves. Another difficulty is that all of the plans proposed to date require a high degree of skill and experience on the part of the testing technician. Thus industry today finds itself in need of a test which will give highly accurate results and at the same time be simple and easy to perform. The purpose of this paper is to describe a new method of testing—termed the "reverse-rotation test"—which will more nearly meet these requirements. The theoretical basis and the assumptions involved are discussed. Tests to determine the accuracy of the method have been made on two squirrel-cage motors of somewhat different characteristics by various methods of testing. The results of these investigations are described and curves are given showing the degree of exactness obtained. While the method includes assumptions regarding compensating effects involving minor components of the loss, its over-all accuracy for the motors tested is shown by comparison with the values obtained by other methods having high precision.

Advantages

While the accuracy of the reverse-rotation method of determining stray load loss has not yet been thoroughly established for all types of induction machines, the results of the experimental work described herein prompts the following claims:

1. This method will give highly accurate results and at the same time the test is simple and convenient to perform.

2. The value of the losses can be determined quickly without the use of special apparatus—the only requirements being a source of power having adjustable polyphase voltage and a driving motor for which the losses can be determined.

3. Laborious computations are avoided and final determinations of the value of the stray load loss can be obtained in a few moments by simple calculations from the measured quantities.

Test Procedure and Computations

The reverse-rotation test is carried out by applying reduced balanced polyphase voltage to the stator terminals of the machine being investigated while driving the rotor at synchronous speed in the direction opposite to that of the revolving stator field. The stator current may be set at any desired value by adjustment of the applied voltage. Two power measurements are necessary: the power required to drive the rotor and the power input to the stator circuit. Let the former be designated by P_r and the latter by W_s. The power required for rotation of the motor rotor (P_r) may be obtained by driving it with a sensitive dynamometer or a motor with known losses. The friction and windage losses at synchronous speed of the motor under test are also required and may be designated by P_f. The difference between these two quantities gives the component of the net input to the rotor which is supplied through the driving mechanical power. If this net power resulting from rotation be represented by P, then:

$$P = P_r - P_f$$

When a d-c motor is used to drive the induction machine, correction for losses in the d-c motor becomes a simple matter if the same value of field current is used for both the load and the no-load determinations, obtaining constant speed by armature voltage control. The difference in the armature-circuit brush and copper losses of the d-c motor is the only correction that must be applied to the difference of the two d-c input values in order to determine the net power P, since all other losses in the driving machine remain constant.

The total stator power input W_s is carefully measured by wattmeters for the particular value of current used. An accurate measurement of the stator d-c resistance is made at the winding temperature used during this power observation. The total polyphase stator copper losses are then computed and may be designated by W_{cu}. When subtracted from the total stator input (W_s) there remains the net stator power which may be called W, and:

$$W = W_s - W_{cu}$$

The value of stray load loss for the particular stator current used is then obtained as the difference between net rotor input and net stator power, or

$$\text{stray load loss} = P - W$$

Experience indicates that it is easier to obtain accurate test points over the range between full-load current and about double this value. If points are determined over this range of current the value of the stray load loss for any desired current can be computed readily by application of the law that the loss varies as the square of the current. The curve for the working range of the motor is obtained simply in this manner.

Light-Load Correction

In order to satisfy the definition of stray load loss it should be zero at no load. This desired result is obtained by calculating the loss for no-load current and subtracting its values at this point, that is, simply making the loss zero at no load. At the one-half load point one-half the correction necessary for no load may be applied, and at full load no correction is necessary.

Discussion of Principles

The stray load loss in induction machines is defined as the residual power

Paper number 39-4, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 17, 1938; made available for preprinting November 28, 1938.

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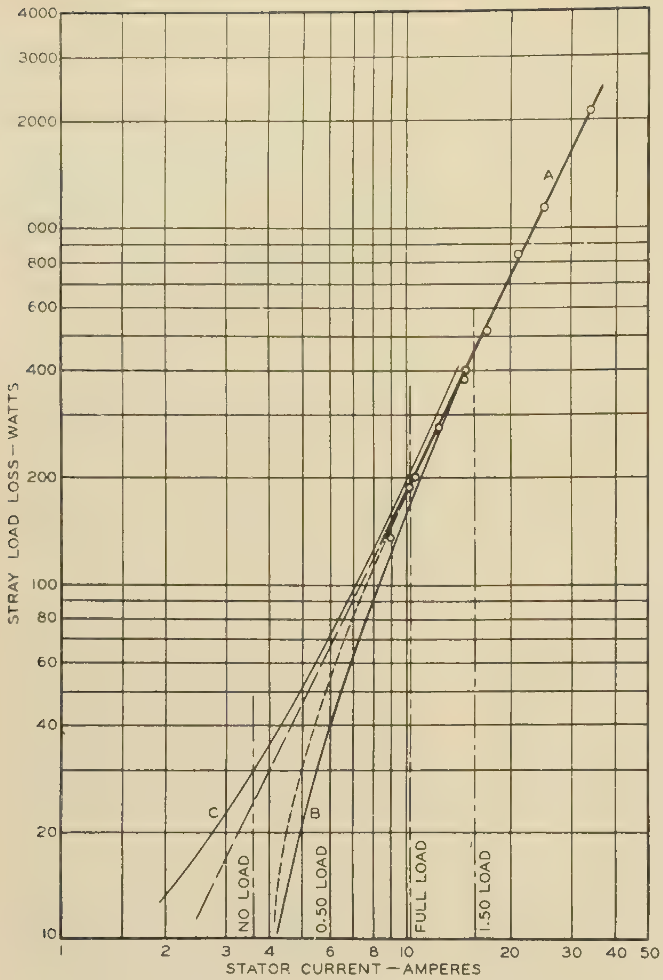

Figure 1. Stray load loss curves for motor 1

A—Loss by reverse rotation method. Dashed line is straight line extension; dotted line shows loss after light-load correction is applied

B—Loss by load-back through belt

C—Loss by load-back through belt, including added component for no-load current

power through the stator and mechanical power supplied through rotation ($W_r + P_s$). After subtracting stator copper losses and friction and windage losses from the total power input (both electric and mechanical) the remaining losses may be classified in three frequency categories: (1) fundamental-frequency losses caused by the leakage flux of the stator, (2) double-frequency losses in the rotor resulting from reverse rotation, and (3) tooth-frequency components of the stray load loss occurring in both rotor and stator. The stator fundamental-frequency losses caused by the leakage flux, which are apparently very small, are not considered at this point and only included in the final result through compensating effects to be explained later. The double-frequency rotor losses result from currents in the rotor conductors and fluxes in the rotor iron at this frequency. With rotation at synchronous speed in opposite direction to the stator field the double-frequency rotor losses are supplied in equal amounts from the power of rotation P and from power transmitted by the stator circuit through the air gap W. The tooth-frequency losses result directly from rotation, and consequently

loss after all other known losses are considered in accordance with specified methods of measurement.[1] The loss consists chiefly in the increase in iron and copper losses occurring as a result of the load current in the stator and rotor circuits while the rotor is revolving. The total stray load loss may have components at tooth frequency and at fundamental frequency, the former being of major importance. The reverse-rotation method of testing gives a measurement of all tooth-frequency components and includes the fundamental-frequency components only through compensating effects. The test procedure also assumes that the direction of rotation of the rotor with respect to the direction of the fundamental flux does not alter the magnitude of the tooth-frequency losses, since in both cases the tooth-frequency fluxes are superimposed upon fundamental-frequency fluxes.

When the rotor of the machine is turned at synchronous speed in the direction opposite to the field flux all power supplied is consumed in losses. The total power input consists of electric

1. For all numbered references, see list at end of paper.

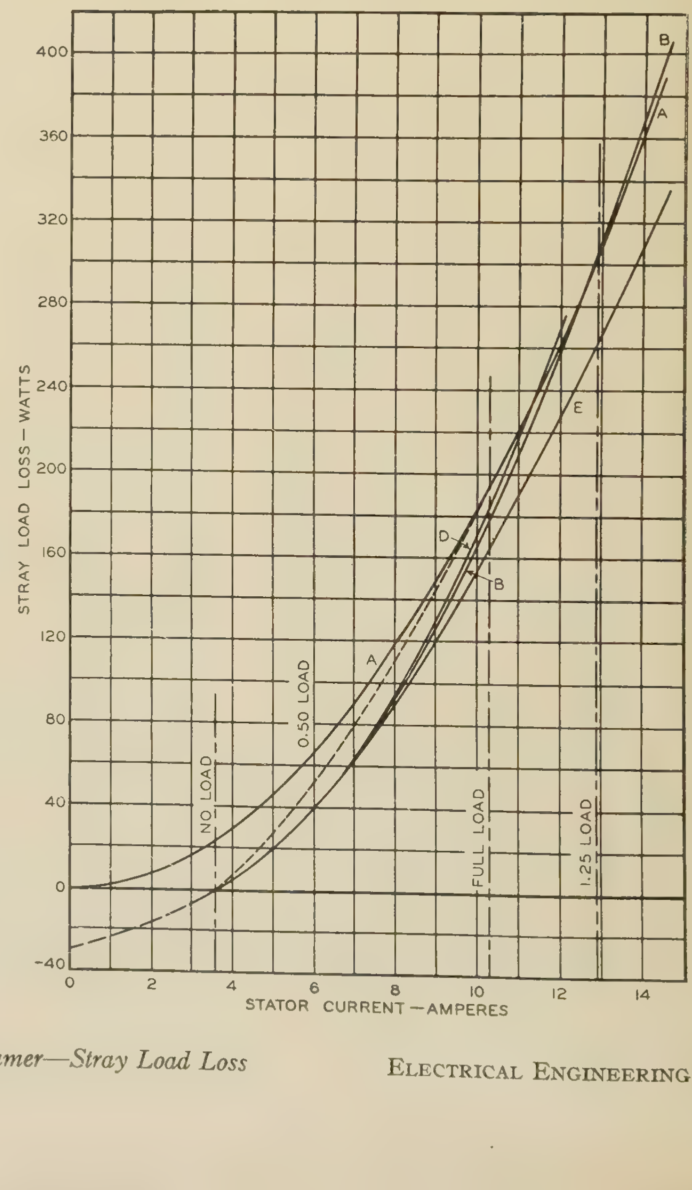

Figure 2. Comparison of stray load losses for motor 1 determined by different methods

A—Loss by reverse rotation method. Dotted line shows loss after light-load correction is applied

B—Loss by load-back through belt

D—Loss by load-back through d-c machines

E—Loss by d-c excitation of stator

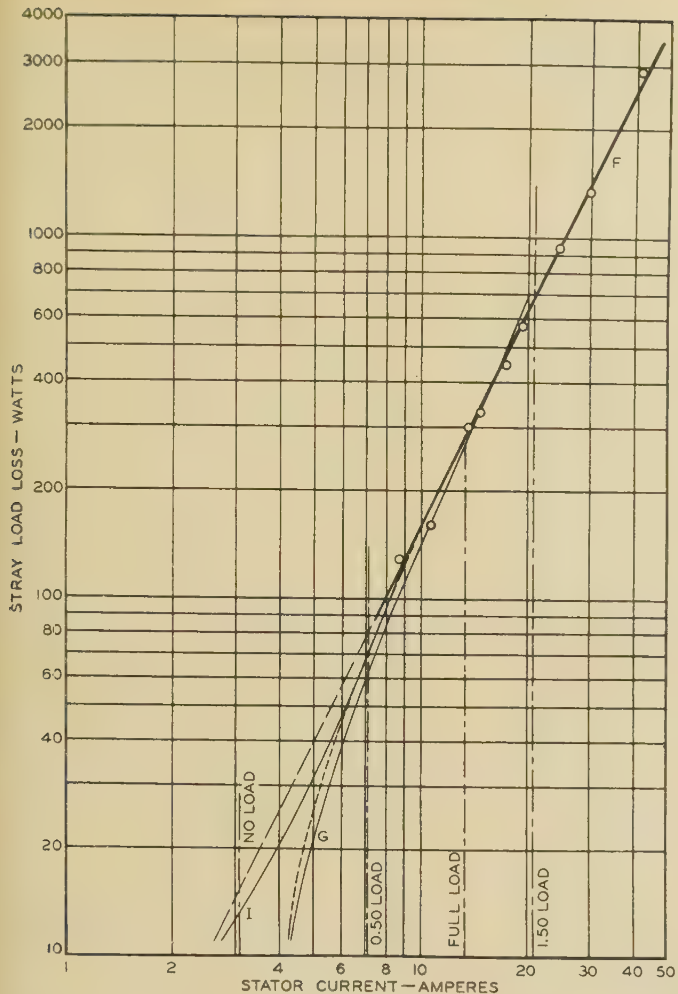

Figure 3. Stray load loss curves for motor 2

F—Loss by reverse-rotation method. Dashed line is straight line extension; dotted line shows loss after light-load correction is applied

G—Loss by load-back through belt

I—Loss by load-back through belt, including added component for no-load current

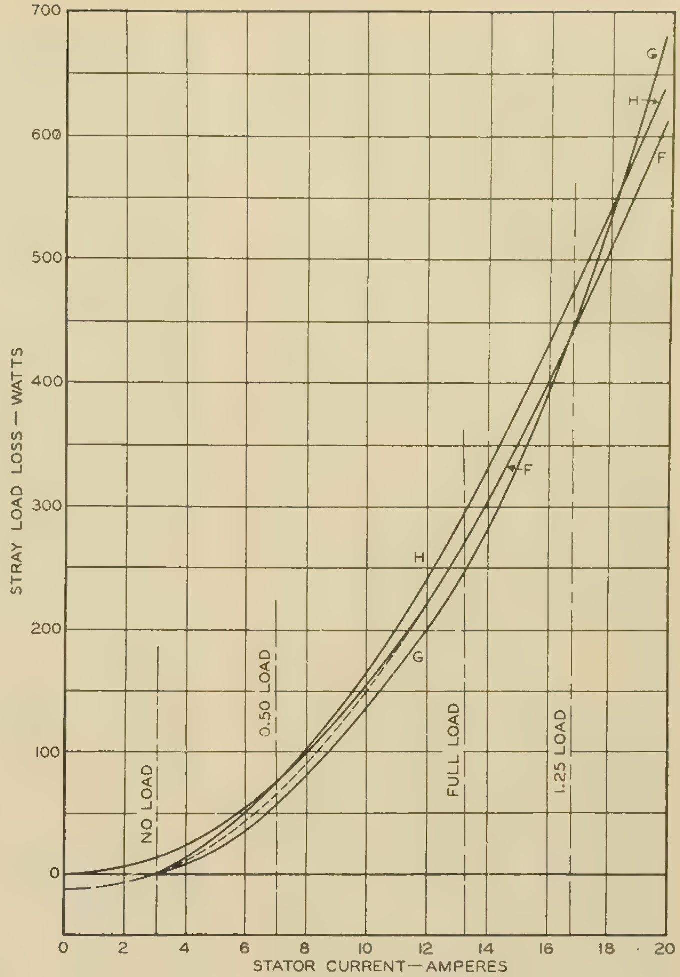

Figure 4. Comparison of stray load losses in motor 2 as determined by different methods

F—Loss by reverse rotation method. Dotted line shows loss after light-load correction is applied

G—Loss by load-back through belt

H—Loss by d-c excitation of stator

are supplied only by mechanical power. Thus the net power input through rotation P consists of one-half the double-frequency rotor losses and the total tooth-frequency losses. By subtracting from this quantity P the air-gap power supplied from the stator W, the tooth-frequency losses are separated out and given as the remainder.

The question may be raised regarding the amount of the error introduced by neglecting the component of the stray load loss which might be assumed to exist because of the fundamental-frequency leakage flux. As a matter of fact, the proposed treatment not only neglects this component of the loss but includes it in the stator value W which is subtracted from the net rotational power input component P, thus doubling this error. However, the fundamental-frequency component of the stray load loss is very small and any error resulting from this treatment is evidently compensated by other small errors involved in the method. These may be listed as follows:

(*a*). The iron of the motor is worked at low saturation during this test which tends to give a higher value of stray load loss than would exist under normal saturation conditions.

(*b*). Synchronous speed is slightly higher than normal speed of operation, giving a slightly larger value of stray load loss (which varies as the square of the speed).

(*c*). In the application of test results to motor performance the currents used during the test are considered as stator input currents under load conditions. During the test, due to the absence of the exciting component, the stator current has a corresponding rotor current, while under actual motor operation the rotor current is less than the stator current. This slight increase in rotor current under test conditions gives a larger value of stray load loss for a given stator current than actually exists under normal operating conditions.

The above items separately appear of minor importance but they all act in the same direction and tend to compensate for the omission of the fundamental-frequency component of the stray load loss, which is evidently a small quantity. Experimental results indicate that this compensation is approximately complete in the region extending from 150 per cent load to well below full-load. At light loads a small correction is necessary, and the purely arbitrary method previously proposed is probably sufficiently accurate for all practical purposes and it has the advantage of being simple and easy to apply.

Test Results

Motor 1

The first motor tested was of the squirrel-cage type and was rated at 10 horsepower, 550 volts, 10.3 amperes, 3 phase, 60 cycles, 1,750 rpm. This motor had

previously been tested with extreme care by several different methods to determine its stray load losses.[4] In figure 1 stray load losses are shown as a function of armature current when plotted on logarithmic cross-section paper. The points of curve A are the actual experimental points, determined in the manner previously described, by the reverse-rotation method over a range from approximately full load current to three times that value. These points fall on a straight line having a slope of two, indicating that the loss varies as the square of the current. This curve extended downward as a straight line is shown by a dashed line and gives values of the stray load loss over the normal range of operation of the motor omitting correction for light loads. The dotted line shows the curve for low values of load current after the correction for light loads is applied. Curve B shows values of a stray load loss for the same motor as determined by careful measurement by the belted load-back method.[3] Curve C is the same as curve B except that it includes the theoretical component of stray load loss which exists at no load.[3,4]

The curves of figure 2 show the same values for curves A and B as in figure 1 when plotted to uniform-scale co-ordinates. The dotted line curve shows curve A corrected for light loads. On this plot values of stray load loss, determined with a high degree of precision by the load-back test through d-c machines, are given by curve D.[4] Values as determined by the d-c excitation method[2] are also given for comparison in curve E. The results shown by curves B, D, and E were given in a previous paper[4] and are repeated here so that the curve A determined by the reverse-rotation method may be compared with them.

The curves of figure 2 show that for motor 1 the reverse-rotation method gives values for the stray load loss that are in as close agreement with those from other methods of testing as exists among the other methods themselves. At the full-load point the reverse-rotation method value is only 7 watts above the highly accurate value obtained by loading back through d-c machines, and 14 watts above the result obtained by the belted load-back method. At 125 per cent load the values from curve A and curve B agree almost exactly. The reverse rotation method gives a value that is about 11 watts high at 50 per cent load.

Motor 2

This motor differed from motor 1 in that it possessed abnormally high iron losses, being 5.0 per cent for this machine as compared with 2.9 per cent for the first motor. It was also of the squirrel-cage type and was rated 10 horsepower, 220/440 volts, 26.6/13.3 amperes, 3 phase, 60 cycles, 1,710 rpm. It was tested for stray load losses by three different methods of testing: the reverse rotation, the belted load-back,[3] and the d-c excitation method.[2] The test points of the reverse rotation method are shown by curve F in figure 3, plotted to logarithmic scales, and again give a straight line having a slope of two. Straight line extension downward is shown by a dashed line as before while the dotted line shows extension after the application of the light load correction. The results of the belted load-back test are shown by curve G, while curve I gives results of this test with the no-load component included.

The results of the three tests over the normal operating range of the motor are shown in figure 4, where curve F is determined by the reverse-rotation method, (the dotted line showing corrected curve for light loads), curve G comes from the belted load-back test, and curve H is derived by application of the d-c excitation method. At the full-load point curve F lies approximately midway between curves G and H being about 23 watts above the belted load-back method value and 24 watts below the value obtained by the d-c excitation method. At 125 per cent load curves F and G agree and curve H is 30 watts higher. The reverse-rotation method gives a value that is about 7 watts higher than that from the belted load-back method at 50 per cent load, and it is about the same amount below the d-c excitation value in this region.

Because of difficulties involved in performing the tests and inaccuracies inherent in each method, complete agreement in results by application of the various methods employed has not been possible. However, the reverse-rotation method has given results for the two motors tested that are well within the degree of accuracy which might be expected from any of the methods used.

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Discussion

L. E. Hildebrand (General Electric Company, Lynn, Mass.): It is known that stray load loss causes the speed-torque curve to depart appreciably from the calculated curve predicted by formulas which do not take high-frequency core loss into account. These losses constitute a part of the load on the motor and hence must reduce the net torque at a given forward speed and increase the gross torque at any backward speed. It should be possible to predict the correction in torque at any speed by an extrapolation of the stray load loss measured at normal speed and load. We have made fairly accurate predictions with quite simple assumptions for extrapolation, namely, high-frequency loss proportional to rotor current squared and frequency to the three-halves power. Agreement at backward speeds and at speeds greater than half forward speed are very good.

The converse is also true, that is, from tested backward rotation torque or tested breakdown torque we can find out what the stray load loss at normal load and speed is. We have used (1) backward rotation, (2) d-c excitation, (3) polyphase breakdown torque, and (4) single-phase breakdown torque, all to measure stray load loss. All agree quite well. Measurement of the single-phase breakdown torque seems to be a very good alternative method to d-c excitation and backward rotation.

William R. Hough (Reliance Electric and Engineering Company, Cleveland, Ohio): The paper under consideration presents a method of study of the stray load losses of induction machines, which is a definite contribution to progress in this field. This discussion is based on a limited experience with the method developed by the authors.

This limited experience has shown that of the advantages claimed by the authors for their method of testing, the degree of accuracy is the only point in question. The tests are simple and convenient to perform, can be done relatively quickly with a minimum of necessary equipment, and the computations of stray load loss from test information are not laborious.

An analysis of the steps taken in arriving at the value of stray load loss for a particular condition readily indicates that a high degree of accuracy is necessary in each test reading obtained, in order that the final result, namely, stray load loss, will be accurate.

The stray load loss is the difference between two quantities, each of which is the difference between two other quantities. In testing a normal induction motor having from one to two per cent stray load loss, it will be found that the stray load loss is

small in comparison with the test values obtained by this method from which it is determined. In testing machines having higher than normal stray load losses, the relative accuracy should be greater, since the stray load loss for any condition would be larger in comparison with the values from which it is obtained.

The authors have pointed out that their method neglects one of the components of stray load loss, and have cited certain compensating items. They have not attempted to evaluate these discrepancies other than to demonstrate that their method gives substantially accurate results by actual test in comparison with load-back test results. The final proof by actual test results is, of course, the most important consideration in determining the value of this method of testing. The authors have, by the limitations they have cited, pointed out the necessity of proof of the method of actual test results, and have contributed two specific cases to support this proof.

The limited number of tests which are the basis of this discussion substantiate the method in principle, but do not substantiate the degree of accuracy obtained by the authors. Present experience would indicate the variance between the values of stray load losses determined by this method and those determined by load-back tests, to be in the order of one per cent of motor input. Continued experience is necessary before a more accurate opinion can be given.

P. C. Smith (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This is the least complicated method so far proposed for squirrel-cage motors. It has no advantage, from a simplicity standpoint, over the Koch method for wound-rotor motors, since the same readings are required for both. The authors have pointed out the errors in principle and the assumptions made in this method and show by test that they are negligible or are compensated for in a ten-horsepower four-pole motor. There are, however, two factors which are increasingly important with increase in motor size and, if neglected, will lead to appreciable error. I have reference to the fundamental end-zone loss and the correction for magnetizing current at full load.

In large motors, particularly high speed, the end-zone loss is an appreciable amount. When the input to the stator is measured, this loss is included. Hence, when stator input is subtracted from rotor input, this end-zone loss serves to reduce the net result when actually it should be added. That is, twice the end-zone loss must be added to the answer obtained by this method to get the correct loss. This loss is partly compensated by neglect of the magnetizing current, but it does not necessarily follow that they balance. In fact, in large high-speed motors, it may lead to considerable error.

It is pointed out in the paper that the absence of magnetizing current, rather the fact that it is low, results in a secondary current which is too high. In low-speed motors, where the magnetizing current is large, some correction at full load will be necessary.

As pointed out in this discussion, this loss and the end-zone loss tend to balance, but the high end-zone loss goes with high-speed motors while high magnetizing current goes with slow speed and only in certain cases will they cancel out.

Further tests, over a wide range of horsepower and speed, are required to check this method.

R. E. Hellmund (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): After listening to the paper by Morgan, Brown, and Schumer and some of the discussions, I believe that in view of the widely different results obtained, better progress could be made in evaluating the various methods under discussion if more data were given regarding some of the important design characteristics of the machines tested. At least it would be desirable to know the speeds, ratings, and various essential facts regarding the slot, tooth, and tooth-tip structures. If some such data were available, it might be possible to draw conclusions as to which of several methods can be used to best advantage for a given range of sizes or types of motors. Undoubtedly it is desirable to find methods giving satisfactory results for all ratings, but such ideal conditions cannot always be obtained and therefore it may be necessary to exercise a certain amount of discrimination.

C. J. Koch (General Electric Company, Schenectady, N. Y.): The test described in this paper is the easiest to perform with accuracy, of the methods so far developed for measuring the stray load loss of induction machines. In short the test procedure itself and the apparatus required become identical with the established short-circuit core-loss test of synchronous machines of similar sizes.

Does the reverse-rotation test determine the value of the stray load loss as it actually exists in the machine under load conditions? If we could calculate accurately the high-frequency core and stray load losses in induction machines this question could be answered from theoretical considerations. This we cannot do, however, and we must resort therefore to tests of large numbers of motors to determine the importance of the disturbing effects mentioned by the authors.

The result of our experience has been that these effects are small. This indicates the principle source of the high-frequency losses to be the action of the tooth harmonic fluxes. As far as the action of the stator-tooth harmonic fluxes on the rotor is concerned their frequency is very nearly the same for normal operation, reverse-rotation test, or normal-speed test with d-c applied to the stator. We have to offer one test confirming this. The motor tested was rated 150 horsepower, 900 rpm, and was of the collector ring type. At rated load the values are:

Stray load loss by pump-back between identical motors .... 615 watts

Stray load loss by reverse-rotation test .. 600 watts

Stray load loss by d-c excitation of rotor .. 600 watts

We have also measured the stray load loss of a number of motors by the reverse-rotation test and by very carefully made dynamometer tests. All of these motors were rated 25 horsepower at 1,800 rpm synchronous speed and were of the squirrel-cage type. The results are shown in table I of this discussion. It will be noted that the stray load loss varied greatly from motor to motor. The reverse-rotation test, however, follows the dynamometer result with very satisfactory agreement at rated load.

Table I

Motor	Stray Load Loss by Dynamometer (Watts)	Stray Load Loss by Reverse Rotation (Watts)
A	637	650
B	230	245
C	400	335
D	195	230
E	487	450
F	712	620
G	130	215
H	1,003	800
I	355	520
J	747	670
K	224	350
L	184	157
M	255	260

These results create confidence in the reverse-rotation test as a measure of stray load loss. The ease of making the test and the fact that one motor only is required should stimulate further testing along this line with the ultimate object of incorporating the reverse-rotation test in the test code.

F. D. Phillips (General Electric Company, Schenectady, N. Y.): The data given in this paper show a very good agreement between the various methods of determining the stray load losses. For the first machine these are, reading from figure 2,

By reverse-rotation method .... 194 watts
By load-back through belt .... 180 watts
By load-back through d-c machines ..... 187 watts
By d-c excitation of stator .... 165 watts

A maximum difference of 29 watts and a variation from the average of 17 watts.

For the second machine the results obtained are, reading from figure 4,

By reverse-rotation method .... 270 watts
By load-back through belt .... 247 watts
By load-back through d-c machines ..... 294 watts

The maximum difference is 47 watts and the variation from the average 24 watts.

The accuracy of the determination of these losses is apparent when we realize that a number of instruments are used and careful investigation has shown that on such tests the best accuracy of the instruments and of observation is in the neighborhood of 30 watts under carefully controlled laboratory conditions.

The results shown in Mr. Koch's discussion of a larger number of comparisons between the reverse-rotation method and the dynamometer method show a fair agreement. The difference between the two methods varies from 5 to 37 watts in seven cases and from 65 to 203 watts in six cases and the machines on which one method gives lower losses are the same in number as those which gave higher losses. These tests were taken under commercial factory conditions and the results would not be expected to be as accurate as those obtained in a laboratory.

The results given in this paper and the discussion lead to the belief that further study will confirm the validity of this testing method. It is attractive because of its simplicity and because it does not require special apparatus, and the calculation of the results is simple.

Q. Graham (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Morgan and his associates have proposed an ingenious method of measuring stray load losses which has a fairly sound theoretical basis. There is a definite need for such a method, particularly for larger motors where other methods of testing are more difficult. The accuracy of the method, however, must be checked with smaller motors for which input-output tests are possible.

I have applied the reverse-rotation test to six motors ranging in size from 3 to 20 horsepower. The stray loss is given in table II of this discussion for 100 per cent and 125 per cent current and is compared with the loss by input-output test. For four of the six motors the reverse-rotation test shows about double the loss given by input-output test while for the other two there is close agreement although the reverse-rotation method still shows the higher values. For the last motor on the list the losses by input-output test were measured at a reduced voltage also and it was found that for the same values of current the stray loss was higher, thus more nearly approaching the loss measured by reverse rotation. This would seem to indicate that the presence of normal saturation may have a more important effect than the authors have assumed and may account for much of the discrepancy shown by these tests. The authors, of course, have a right to question the accuracy of the input-output tests which have been used for comparison in the results presented here. I can only say that they are the result of a great deal of painstaking work in perfecting a testing procedure in which we ourselves have considerable faith. The comparative tests are an honest attempt to judge the merits of the proposed method but are by no means presented as final evidence of its failure.

Table II

Motor Number	Horsepower	Poles	Per Cent Current	Watts Stray Load Loss: Input-Output	Reverse Rotation	Direct Current (Code)	
1	$7^1/_2$	2	100	225	500		
			125	400	720		
2	$7^1/_2$	2	100	240	425	375	
			125	405	620	600	
3	20	2	100	355	790		
			125	700	1,220		
4	$7^1/_2$	4	100	135	145	160	
			125	205	220	239	
5	15	4	100	415	495		
			125	615	785		
6	3	2	100	35	90		220 volts
			125	62	142		220 volts
6	3	2	*100	50	90		180 volts
			*125	89	142		180 volts

* Losses recorded are for same amperes as for 220-volt test.

Victor Siegfried (Worcester Polytechnic Institute, Worcester, Mass.): The authors are to be commended on the development of a method which is simple and is performed with the most fundamental equipment. Having had experience in some of the previous work on one of these same machines in which the stray load loss was determined by loading-back tests, I can confirm the claims made for this present test as to the ease with which it is performed. The relative amounts of time consumed in the two tests are illustrated by the fact that a whole run was made and calculated up into final results in about the same time required for one point in the previous tests. It is further interesting to note that all that is required in this new method is some machine to drive the motor at synchronous speed backward and a source of reduced potential at rated frequency, in the order of 20 per cent of the original ratings of the machine on test, depending upon its constants.

The test itself is particularly noteworthy in that it measures the losses of the machine under conditions of loading although at reduced excitation, and the separation of these losses is very simple as outlined in the body of the paper. This gives a test whose final results for stray load loss are substantially in agreement with other tests of proved accuracy, and even though there are inherent errors in the method, the authors have logically demonstrated that these errors tend to compensate for each other.

T. H. Morgan: The discussion on this paper is on the whole encouraging. In considering a test method of this kind it would be unfortunate if it were to meet with complete approval before receiving the necessary verification covering a complete range in machine type and size. The fact that several tests have already been made and reported on in the discussions indicates interest in the matter. It is our hope that this interest will continue so that the limitations of the test and its degree of accuracy may be definitely established.

It is the authors' opinion that the industry possesses too little knowledge regarding these losses, both as to the mechanism that produces them, and their exact location in the machine. As an example, P. C. Smith believes that an end-zone loss takes place in the stator of large high-speed motors, thus decidedly limiting the accuracy of the method described; that if this end-zone loss is an appreciable amount of the total stray load loss, the proposed treatment would produce a result considerably too low in amount. This reasoning seems correct. On the other hand, the results obtained by Q. Graham would indicate that the reverse-rotation method gives a value of loss which is high in all cases of motors of two poles tested by him. There are several similar conflicting differences embodied in the discussion and this one is mentioned only to illustrate the point that as yet we do not know which view is the correct one. The discussion indicates the need of a better understanding of the nature of stray load loss if we are to make progress in reducing it by improvement in design.

The apparent lack of agreement between the results of tests made by different people to determine the accuracy of the proposed method is not surprising. The experience of the authors has been that it is very difficult to secure the same measured value of the loss from any two of the recognized standard methods of testing. Those who work on the problem of stray-load-loss measurement will always agree on one point—namely, the difficulty of making accurate determinations. It therefore seems hardly sufficient to compare the results of the reverse-rotation test with values obtained by only one other method. It takes more time and energy to use several methods but a much more accurate comparison will result. In this connection the test results obtained by C. J. Koch on the 150-horsepower, 900-rpm motor are encouraging.

The suggestion of R. E. Hellmund that design information be given whenever possible is well taken. Only through a large number of tests on motors of different design will it be possible to determine the limiting point where a new method of testing fails to give sufficiently accurate results. It is quite possible that any one method may apply to only a certain range in size and type of motors. If so it would be advantageous to know the limitations. Design data regarding motor number 1 of the paper were previously given in reference 4. Exact design details of the other motor are not known but it can be said that they are similar in character.

The remarks of L. E. Hildebrand regarding the application of the laws of variation of stray load loss as a function of current and speed are pertinent. The authors have made investigations of these effects over the complete range of reverse rotation of the motor, and plan to have their findings ready for publication in the near future.

In closing the discussion it should be again pointed out that the authors feel that while the general results from the limited number of tests taken to date indicate possible high accuracy for the reverse-rotation method of testing, many more tests should be made employing different methods of testing before a satisfactory conclusion can be reached.

Inductive Co-ordination With Series Sodium Highway Lighting Circuits

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Synopsis: This paper describes the wave-shape characteristics of the sodium-vapor lamp and discusses the relative inductive influence of various series circuit arrangements in which such lamps are employed. A method is outlined by means of which the noise to be expected in an exposed telephone line may be estimated. Measures are described which may be applied in the telephone plant or in the lighting circuit to assist in the inductive co-ordination of the two systems. These measures need be considered only when a considerable number of lamps is involved, since noise induction is negligible when there are only a few lamps as, for instance, at highway intersections.

During the past few years, a new type of lamp has been developed for highway lighting purposes, making use of ionized sodium vapor as its luminous element. While lamps of this type can be operated in parallel, the most common application has been in the conventional series types of lighting circuits supplied through constant-current transformers.

The wave-shape characteristics of sodium-vapor lamps are such that where series lighting circuits supplying a considerable number of such lamps are involved in exposures with open-wire telephone lines, attention must be given to the co-ordination of the two systems from the noise standpoint. The present paper gives the results of an investigation of the various factors involved in situations of this character. It is based largely upon a study conducted by project committee 1*A*, on noise induction, of the Joint Subcommittee on Development and Research of the Edison Electric Institute and the Bell Telephone System.

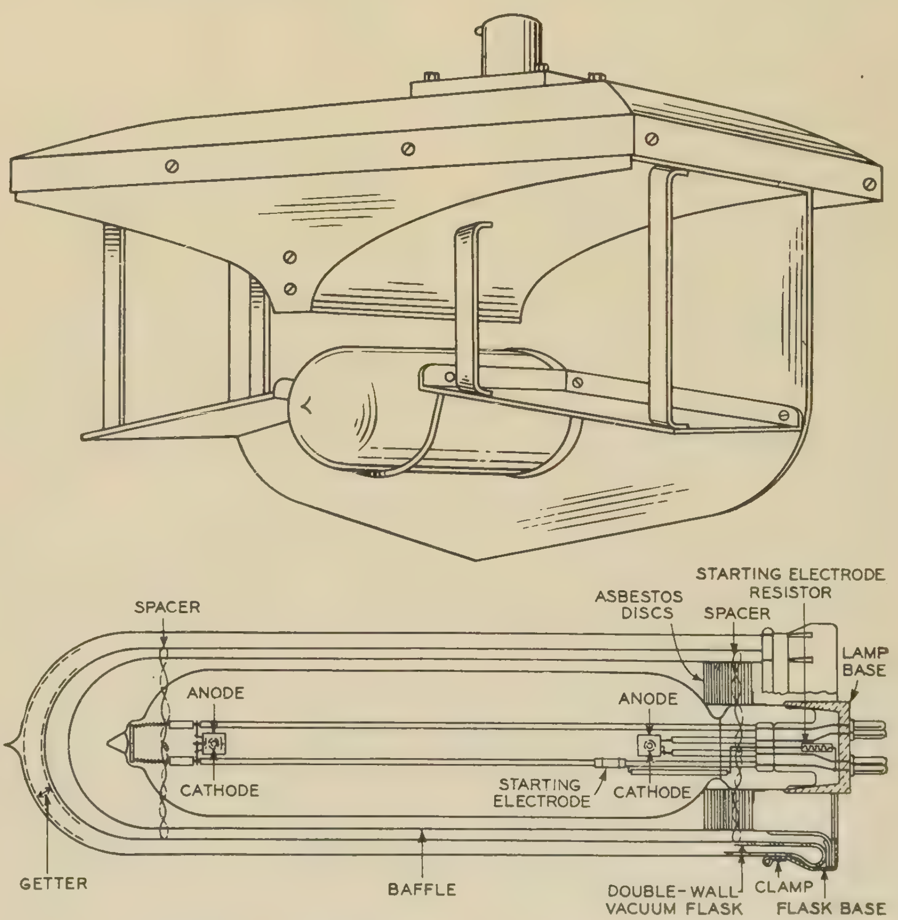
The wave-shape data included herein were obtained in the laboratory of one of the manufacturers and, through the co-operation of one of the power companies, on a number of field installations of sodium lamps of the same manufacturer. One of the situations tested, in which a lighting circuit some 16 miles in length was involved in an exposure with an open-wire telephone toll line, afforded an opportunity for co-ordinated inductive influence and noise measurements.

Operation of Sodium-Vapor Lamps

A brief review of the construction and operation of the sodium-vapor lamp[1,2] may be of interest as a preface to the wave-shape discussion which follows. The essential details of the sodium lamp designs of two of the manufacturers, together with sketches of the luminaires in which they are mounted, are shown in figures 1 and 2. The following is a brief description of the lamp shown in figure 1.

The lamp consists of a long evacuated glass bulb enclosing, at each end, a tungsten filament or cathode and an open-end molybdenum anode. Each anode is connected to one side of the adjoining filament, the leads to the latter passing through a seal at one end of the bulb to a four-prong tube base which, in turn, makes contact with the socket. The luminous arc occurs between the anode at one end of the tube and the cathode at the opposite end. The connections are so arranged that the two anode-cathode combinations function alternately as the sign of the impressed voltage becomes alternately positive and negative. As indicated in figure 1, the anode-cathode assemblies are symmetrically located in the tube. Differences which occur between the positive and negative portions of the arc voltage curve in a particular lamp, as discussed hereinafter, are therefore due to vagaries of the arc rather than mechanical dissymmetries in the lamp. The bulb is insulated from the outside air by an evacuated glass bottle similar to a Dewar flask. The flask is required to retain heat generated by the arc for vaporizing sodium, which is solid at room temperature. Co-ordinated designs of flask and lamp are required to minimize the effects of external temperatures on the tempera-

Figure 1. Ten thousand-lumen sodium lamp and luminaire—manufacturer A



Paper number 39-33, recommended by the AIEE committees on communication and power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 18, 1938; made available for preprinting December 21, 1938.

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1. For all numbered references, see list at end of paper.

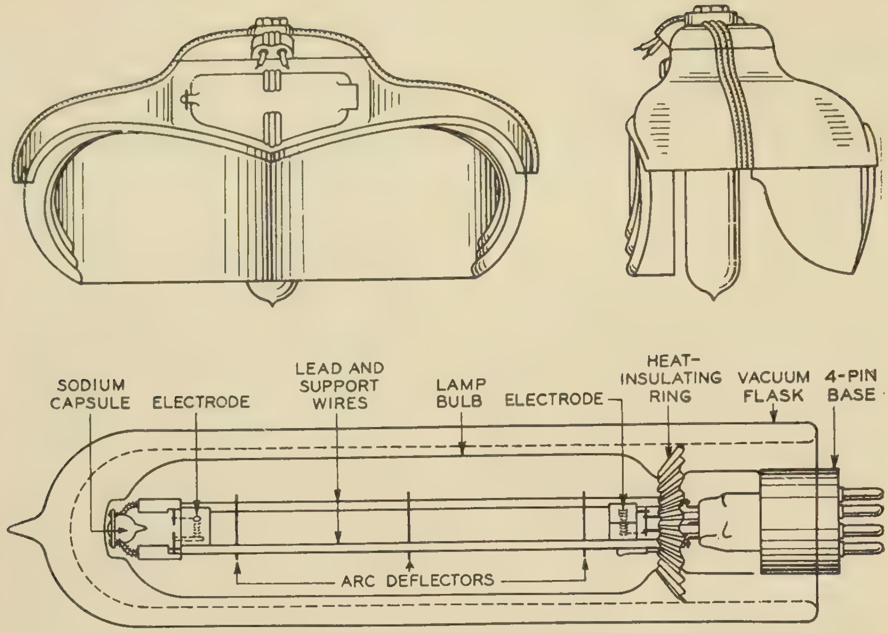


Figure 2. Ten thousand-lumen sodium lamp and luminaire—manufacturer B

ture within the lamp, and consequently on the light which is produced. Neon at a few millimeters pressure (for starting) is included in the bulb.

During the first few seconds after a lamp of this type is energized, the anodes are short-circuited by a time relay. During this period the normal current of 6.6 amperes is passed through the two filaments in series providing a degree of preheating. The short-circuiting contacts then open and a voltage sufficient to ionize the gas is applied to the anodes, the current through the arc and the filaments in series being maintained at 6.6 amperes. The lamp then glows brilliantly with the characteristic neon color. As heat is accumulated within the bulb, the sodium is gradually vaporized and the discharge acquires the characteristic yellow color of the sodium arc. Full light output is reached in about 30 minutes.

In series operation, regulation of the current is accomplished by the conventional constant-current transformer which has a relatively high leakage reactance. In multiple-circuit operation this regulation is effected by means of a special high-leakage-reactance transformer located in or near the luminaire.

Inductive Influence of Series Lighting Circuits

Wave-Shape Characteristics of the Sodium Lamp

Since series sodium lighting circuits are always supplied through transformers having a high leakage reactance, the series impedance to harmonic currents is very high. Consequently the current wave approaches sinusoidal form. Measurements on several installations have indicated values of current TIF (telephone influence factor) ranging from four to ten.

It is characteristic of an arc such as that produced in a sodium lamp that, once the arc has been established, the voltage drop tends to be constant irrespective of the current. This gives rise to a flat-topped voltage wave, more nearly square than sinusoidal in form. This is illustrated by the oscillogram in figure 3, which was taken on a circuit employing 18 sodium lamps in series.

A harmonic analysis of the voltage wave across a single sodium lamp is given in the second column of table I. Only harmonic components in the range up to 1,500 cycles are listed although the measurements indicated the presence of a practically continuous band of harmonic frequencies extending well above 3,000 cycles. The magnitudes of the various harmonics, particularly those at the higher frequencies, varied over a considerable range from time to time. The figures in the table represent average values observed over a relatively short interval. It will be noted that many of the even as well as the odd harmonics were present, indicating that the positive and negative halves of the voltage wave were not exactly alike. In the case of this particular lamp, some of the higher even harmonics were at times, larger than the adjacent odd harmonics and controlled the voltage TIF.

In a series sodium lighting circuit the various lamps can be considered as serially connected harmonic generators. The equivalent series reactance of the supply transformer is high compared to the impedance of the lighting circuit including the lamps. Furthermore, in circuits of the lengths under consideration, attenuation and phase shift are not important. If all lamps were identical, therefore, the per cent harmonic voltages and the voltage TIF at the supply end of a long circuit would be the same as for a single lamp. The $Kv{\cdot}T$ (kilovolts × voltage TIF) would be equal to that for a single lamp multiplied by the number of lamps.

Figure 3. Wave form of current and voltage taken on circuit with 18 sodium lamps in series

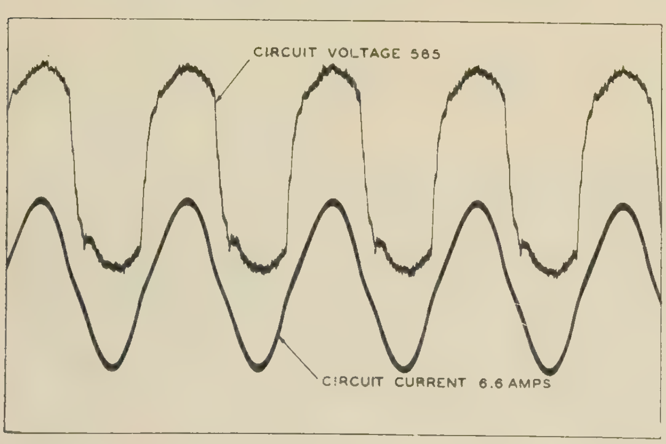
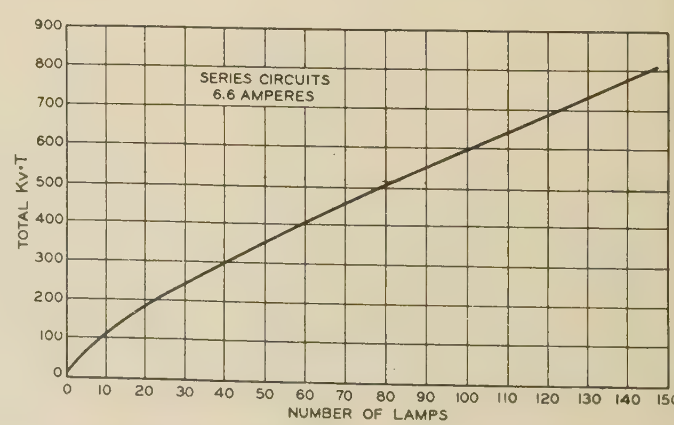


Figure 4. Total Kv · T for sodium lighting circuits having various numbers of lamps



The fourth column of table I gives a harmonic analysis of the voltage as observed at the supply end of a series lighting circuit comprising 149 lamps. In general, the odd harmonic voltages (in per cent) observed on the long circuit are somewhat smaller than those measured on a single lamp, indicating some differences in the relative magnitudes and phases of the odd harmonics generated in the individual lamps. The even harmonics are greatly reduced in the long circuit and are not important contributors to the voltage TIF. This indicates that the even harmonics generated in the individual lamps are fortuitous in character, tending to cancel when a number of lamps are connected in series. Additional data taken on operating circuits of various lengths and showing the variation of the total $Kv \cdot T$ with the number of lamps in series have been plotted giving the curve shown in figure 4.

Distribution of Balanced Kv·T Along the Circuit

Since each lamp of a series circuit acts as a serially connected generator of harmonic voltages, the $Kv \cdot T$ across the circuit changes at each lamp, decreasing progressively from a maximum at the supply end to the value for a single lamp at the distant end of the circuit. While no experimental data are available on the rate of change in the influence, it is probable that a plot of the influence against distance from the far end of the circuit would have a shape similar to the curve in figure 4. In estimates of noise in the practical case, however, a straight-line variation is usually assumed.

Table I. Analyses of Voltage on Sodium Lighting Circuits

	Single Lamp Current = 6.6 Amperes. Root-Mean-Square Voltage = 30 Volts		Series Circuit, 149 Lamps Current = 6.6 Amperes. Root-Mean-Square Voltage = 4,850 Volts	
Frequency	Per Cent of Root Mean Square	TIF	Per Cent of Root Mean Square	TIF
120	*	*	*	*
180	21.9	3	13.9	2
240	*	*	*	*
300	12.9	27	8.7	18
360	*	*	*	*
420	6.1	36	4.2	25
480	*	*	0.6	5
540	2.55	32	3.18	40
600	*	*	0.54	9
660	0.53	12	2.70	61
720	1.82	54	0.43	13
780	1.98	81	1.82	74
840	1.98	109	0.33	18
900	0.83	60	0.61	44
960	1.66	156	0.20	19
1,020	0.41	48	0.14	16
1,080	1.66	199	0.14	17
1,140	0.99	110	0.54	60
1,200	1.82	173	0.17	16
1,260	0.73	58	0.62	49
1,320	0.64	42	0.15	10
1,380	2.12	116	0.80	44
1,440	0.89	42	0.18	9
1,500	1.16	51	0.52	23
TIF	500–625	400**	166.	155**
$Kv \cdot T$	15.3-17.4		805.	

* Value measured controlled by adjacent harmonics or "background."

** TIF calculated from analysis up to 1,500 cycles.

It has been found that the leakage reactance of the constant-current transformers used to supply series circuits of this type is usually sufficiently high to prevent the transfer of the voltage distortion from the lighting circuit back into the supply circuit.

Magnitude and Distribution of Residual Kv·T

In the case of a single-wire ground-return circuit, the residual $Kv \cdot T$ at any point is, of course, the total $Kv \cdot T$ of the circuit at that point.

On a two-wire circuit where both wires are on the same pole line, the magnitude and distribution of the residual voltage† depend upon the number of lamps, the location of the lamps, and the location of any ground which may be on the circuit. Lighting circuits of the usual lengths are in general electrically short so that the series impedances and the capacitance between wires have a relatively small effect. In the case of a circuit isolated from ground, the voltages to ground are determined by the location of the lamps and the capacitances to ground of the circuit conductors.

Figure 5 shows schematically a number of circuit arrangements and indicates the variation, along the circuit, of the voltages to ground and the residual voltage.

† Vector sum of the voltages to ground of each wire.

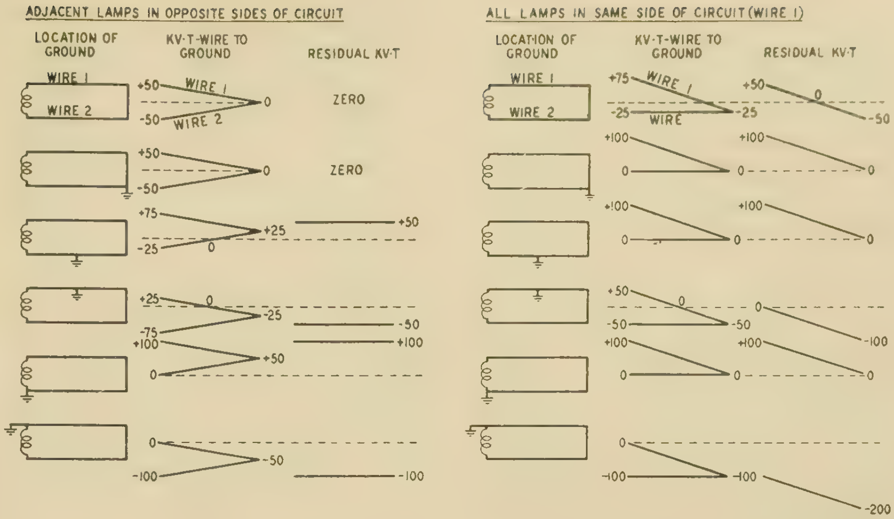


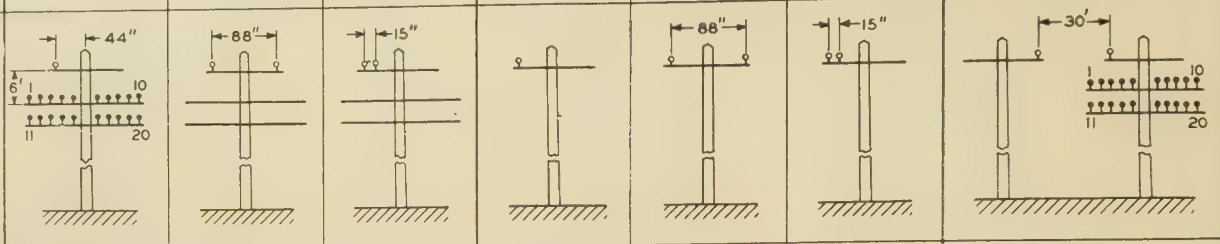
Figure 5. Distribution of Kv · T along a series sodium lighting circuit

Assumptions: capacitance and leakage to ground of the two wires the same; all lamps of identical characteristics

Note: Numerical values indicate percentages of $Kv \cdot T$ at transformer terminals

The smooth variations in these quantities shown in this figure would occur only for lamps extremely close together. In practice there is, of course, an appreciable distance between lamps, and the curves under such conditions consist of a series of steps, the influence being constant over each interval between lamps. The diagrams in figure 5, also involve the assumption that the wave-shape characteristics of all the lamps are identical.

It is evident from figure 5 that the lowest residual voltage occurs where the lamps are staggered, that is where adjacent lamps are in opposite sides of the circuit, and where the circuit is ungrounded or grounded at a balanced point only. For a large number of lamps spaced at finite distances and arranged as above, the residual voltage in each interval between lamps is, theoretically, one-half the voltage generated by a given lamp. The sign of this voltage is opposite in adjacent intervals. In practice, however, there may be considerable differences in the magnitudes and phases of the harmonics resulting from different lamps as well as differences in the capacitance to ground of the two wires (due to the presence of other conductors, etc.) or even differences in the leakance of the two wires to ground. The theoretical reductions in residual voltage due to staggering the lamps, therefore, may not be fully realized. In one case tested, involving an ungrounded circuit 6.6 miles long with staggered lamps, the residual $Kv \cdot T$ at the supply end was found to be about 13 per



INDUCTION FROM RESIDUAL OR BALANCED VOLTAGE →		CASE						
		1	2	3	4	5	6	7
		RESIDUAL	BALANCED	BALANCED	RESIDUAL	BALANCED	BALANCED	BALANCED
LIGHTING CIRCUIT →		GROUND RETURN	METALLIC	METALLIC	GROUND RETURN	METALLIC	METALLIC	METALLIC
SPACING →			WIDE	NARROW		WIDE	NARROW	WIRES AT ROADWAY SPACING
		JOINT USE			ROADWAY SEPARATION **			JOINT USE AND ROADWAY
CIRCUIT		METALLIC NOISE IN NOISE UNITS						
SIDE	1-2	70	24	17				
	3-4	2	16	8				
	5-6	55	55	6				
	7-8	30	16	2				
	9-10	25	24	2				
PHANTOM	1-4	200	100	46	105	12	5	50
	7-10	4	100	0	12	1	0.5	8
	5-16	230	0	24	60	7	3	90
		LONGITUDINAL NOISE IN NOISE UNITS						
SIGMA	1-20	14	0	0.4	2.5	0.3	0.2	6

* FOR 2-WIRE CIRCUIT, BALANCED KV·T BETWEEN WIRES; FOR GROUND RETURN CIRCUIT, RESIDUAL KV·T FROM WIRE TO GROUND

** 30-FOOT SEPARATION BETWEEN TELEPHONE AND LIGHTING CIRCUIT WIRES, WITH GROUPS 1-4 NEAREST LIGHTING CIRCUIT

Figure 6. Calculated noise induction in untransposed telephone circuits exposed to a series sodium lighting circuit

cent of the balanced $Kv{\cdot}T$. While direct measurements of the residual voltages were not made at other points along the line, tests under various grounding conditions indicated that the distribution of the residual $Kv{\cdot}T$ was similar to that shown in the upper right-hand diagram of figure 5. A comparison of the value of 50 per cent for residual $Kv{\cdot}T$ shown in this diagram (for lamps all in one side of the circuit) with the measured value of 13 per cent indicates a four-to-one reduction in the maximum value obtained by the staggered arrangement of lamps. The net effect is small near the center of the line, since the residual $Kv{\cdot}T$ approaches zero for either lamp arrangement.

As indicated in figure 5, when the lamps are staggered, the presence of a ground at any point other than at the far end of a series circuit (or at the midpoint of the supply transformer) greatly increases the residual voltage. A practical example of such an effect was experienced in one situation where an accidental ground on one side of a six-mile lighting circuit increased the phantom-circuit noise on an exposed telephone toll line from about 400 noise units to the order of 2,000 noise units.

In some cases lighting loops are laid out on an all-metallic basis but with the outgoing and return wires quite widely separated. Near the supply end of either the outgoing or the return wire, the residual voltage (on one wire), for a circuit having the lights uniformly spaced, is about half the total voltage across the transformer. This may be seen by a reference to the upper figure in the left-hand column of figure 5. The residual $Kv{\cdot}T$ is, in this case, the $Kv{\cdot}T$ to ground on the particular wire involved.

Inductive Coupling

Since distortion of the current wave form is not an important factor in the co-ordination of series sodium lighting circuits and exposed telephone circuits, no consideration need be given to magnetic induction from the load current. Furthermore, since the length of circuit is relatively short, the effect of the ground-return charging current resulting from the action of residual $Kv{\cdot}T$ can usually be neglected, especially if the circuit is reasonably well balanced to ground. Noise resulting from electric induction from the harmonic voltages on the lighting circuit is, therefore, the only component of importance. It depends upon the relative magnitudes of the balanced and residual $Kv{\cdot}T$, the configuration of the exposure, and the number and locations of telephone circuit transpositions.

If either circuit is in metallic-sheathed cable, the shielding effect of the sheath practically eliminates the induction.

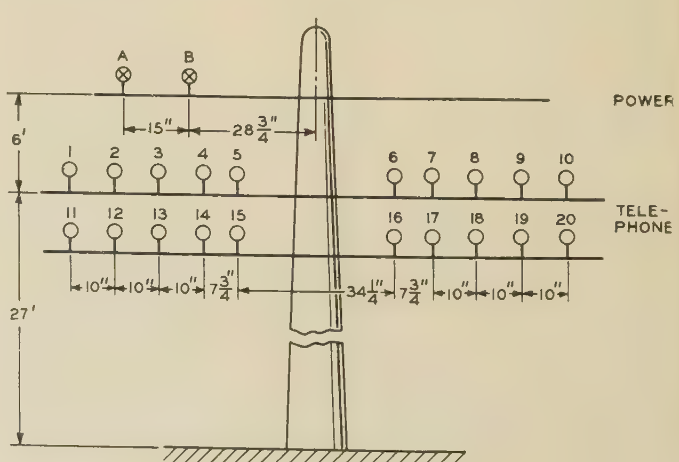
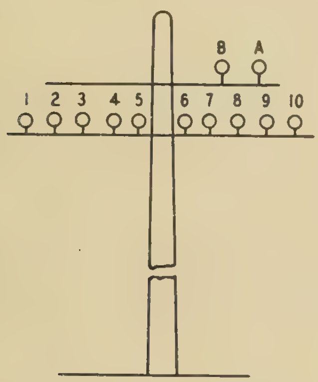


Figure 7. Exposure configuration

Exposure Configuration

In order to indicate the relative influence of various configurations, calculations have been made of the noise induction for a one-kilofoot uniform section of exposure, in which the average lighting circuit $Kv \cdot T$ was taken as 100 and throughout which the telephone circuits were assumed untransposed. These noise induction data are shown in figure 6. The noise-metallic values are for a circuit terminated at each end in its characteristic impedance. The noise-longitudinal values are totals, and will divide in the two directions from the exposure inversely as the respective longitudinal circuit impedances.



$N_{nA} = 0.1 \cdot C_{nA} \cdot K_f \cdot Kv_{g_A} \cdot T$ NOISE UNITS

$N_{nB} = 0.1 \cdot C_{nB} \cdot K_f \cdot Kv_{g_B} \cdot T$ NOISE UNITS

$N_n = N_{nA} + N_{nB}$ NOISE UNITS

$N_{MS} = 0.25\,(N_a - N_b)$ NOISE UNITS

$N_{MPh} = 0.2\,[(N_a + N_b) - (N_c + N_d)]$ NOISE UNITS

$N_L = 0.5\,(N_a + N_b) \cdot 10^{-3}$ NOISE UNITS

N_{nA}—Noise current in telephone wire n due to induction from voltage to ground of lighting-circuit wire A

N_n—Total noise current in telephone wire n due to induction from voltage to ground of both lighting-circuit wires A and B

N_{MS}—Metallic-circuit noise in side circuit terminated in its characteristic impedance (N_{MPh} same for phantom circuit)

N_L—Noise-longitudinal per wire

C_{nA}—Direct capacitance between wire n and wire A, micromicrofarads per kilofoot

K_f—Length in kilofeet of section of uniform configuration

$Kvg_A \cdot T$—Average value, in a section of uniform configuration, of product of voltage to ground of wire A in kilovolts and its TIF

a, b, c, d—Subscript letters indicating the four wires of a phantom circuit, as 1, 2, 3, 4 or 7, 8, 9, 10

Figure 8. Formulas for estimating noise in open-wire telephone circuits in joint use with a series sodium lighting circuit

(Applying to sections of uniform configuration)

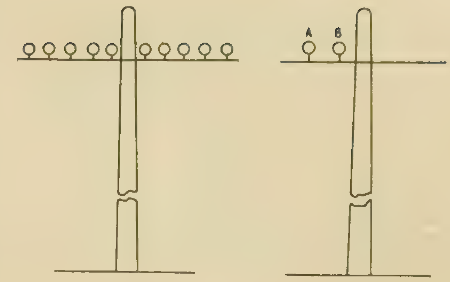
The advantage of a closely spaced all-metallic circuit with staggered lamps over a ground-return lighting circuit, at joint-use separations, may be observed by comparing cases 1 and 3 of figure 6. These figures indicate an advantage of between 5:1 and 10:1 in favor of the metallic circuit. This applies, of course, to the average noise conditions across the lead. A comparison of cases 2 and 3 indicates that, except for longitudinal circuit induction, most of the advantage of the metallic circuit is lost if the two wires are widely separated on the crossarm. The figures given for cases 2 and 3 assume the residual $Kv \cdot T$ to be negligible due to the staggering of the lamps. If a residual $Kv \cdot T$ of ten per cent of the $Kv \cdot T$ between wires were assumed, the figures for case 3 would be increased, on an average, by about ten per cent. However, this would not greatly decrease the advantage of the two-wire balanced circuit over the ground-return arrangement.

At roadway separation the data of figure 6 show the two-wire closely spaced arrangement (case 6) to compare even more favorably with the ground-return circuit (case 4). Even the wide-spaced two-wire circuit (case 5) has a decided advantage over the ground-return circuit at a 35-foot separation. Here again the figures given for the two-wire circuits at roadway separation neglect the effects of residual voltage. If a residual $Kv \cdot T$ of ten per cent were assumed, the figures for case 6 at roadway separation would be increased in a ratio of about 2:1. However, the advantage in favor of the two-wire narrow-spaced circuit as compared to the ground-return circuit would still be of the order of 10:1.

A two-wire arrangement in which one wire is located on each side of the road (case 7) is about equivalent, from the noise standpoint, to a ground-return circuit located across the road from the telephone line.

Telephone Circuit Transpositions

Telephone circuit transposition systems are of maximum effectiveness when (1) the power circuit influence is constant throughout the exposure, (2) the exposure configuration is uniform, and (3) neutral points in the telephone transposition layout occur at the ends of the individual exposure zones. In the case of exposures to series sodium lighting circuits, there is a continuous variation in influence from a maximum at one end to zero at the other end of the lighting circuit. However, if the second and third conditions just mentioned are realized, the variation in influence may not seriously



$N_{ab} = 0.1 \cdot C \cdot K_f \cdot Kv \cdot T$ noise units

$N_{MPh} = 0.2\,(N_{ab} - N_{cd})$ noise units

N_{ab}—Longitudinal noise current in telephone pair ab due to induction from lighting circuit voltage*

N_{MPh}—Metallic-circuit noise in phantom circuit terminated in its characteristic impedance

C—Longitudinal circuit coupling capacitance* between a telephone pair and the lighting circuit—micromicrofarads per kilofoot

$Kv \cdot T$—Average value, in a section of uniform configuration, of product of lighting-circuit voltage* and its TIF

a, b, c, d—Subscript letters, indicating the four wires of a phantom circuit, as 1, 2, 3, 4 or 7, 8, 9, 10

K_f—Length in kilofeet of section of uniform configuration

*Note: If lighting circuit is unbalanced to ground, carry through computations both for balanced and residual voltage

Figure 9. Formulas for estimating noise in open-wire telephone phantom circuits exposed at highway separation to a series sodium lighting circuit

(Applying to sections of uniform configuration)

impair the transposition effectiveness. This is because the unneutralized induction in one portion of a transposed section will generally be opposed by that in a succeeding portion. The length of section in which this takes place depends upon the transposition system and also upon the particular circuit under consideration. Thus in the exposed line system,[3] neutralization takes place between successive eight points for side circuit 1–2, while for side circuit 3–4 a half section is required. On the other hand, in the K-8 phantomed system the transposition pattern is more complex and such simple relations do not hold. This effect is illustrated by the calculated values of noise given in table II for idealized exposure conditions. A uniform joint-use exposure, having the configuration shown in figure 7, was assumed between a 6.5-mile ungrounded lighting circuit and a 20-wire telephone line. The influence of the lighting circuit was assumed balanced with the $Kv \cdot T$ varying uniformly from

Table II. Calculated Noise in a Six-Mile Idealized Joint-Use Exposure

	Untransposed	Transposition Section A	E	KA
Side circuit 1–2	2,200	25	0	15
Side circuit 3–4	950	50	80	15
Phantom circuit 1–4	5,800	1,900	0	0

800 to zero from one end of the exposure to the other. The telephone transposition arrangements within the six-mile exposure were taken as (1) untransposed, (2) the first six miles of an eight-mile A section (standard system[3]), (3) first six miles of an eight-mile E section[3] (exposed line system), and (4) a six-mile KA section[3] (K-8 phantomed system).

The magnitude of the noise for phantom circuit 1–4 in the case of the A section results largely from the fact that in this type of section the quarter points are not neutral points for phantom 1–4.

The degree of neutralization indicated by the tabulated values for the E and KA sections would not be expected to obtain in a practical joint-use exposure of this character, because of the effect of inevitable departures from absolute uniformity of exposure conditions and because of other differences from the idealized conditions assumed. It does appear, however, that in exposures likely to be encountered in practice the variation of the influence along the lighting circuit will be of less importance than the degree of uniformity of the exposure, the relative locations of the ends of the individual exposure zones, and the neutral points in the telephone transposition layout.

Summary of Conclusions

The following is a summary of the more significant facts brought out in the above discussion including an outline of measures which have been found effective in the noise-frequency co-ordination of series sodium lighting circuits and paralleling telephone lines:

1. The wave form of the current on a series lighting circuit supplying sodium lamps is approximately sinusoidal. The voltage wave, however, is distorted and somewhat irregular in character, roughly approximating a square wave (see figure 3). An analysis of the voltage wave indicates the presence of all the odd and many of the even harmonics of the fundamental supply frequency.

2. The wave-shape distortion on the lighting circuit is not transferred to the circuit supplying the constant-current transformer because of the relatively high leakage reactance of the latter.

3. Since only the voltage wave is distorted, electric induction in exposed telephone circuits is the only type of importance. Consequently, if either the lighting circuit or the paralleling telephone circuits are in metallic-sheathed cable, the shielding effect of the sheath will prevent appreciable noise induction.

4. The line-to-line $Kv{\cdot}T$ varies from a maximum at the constant-current transformer to a minimum at the far end of the circuit. The maximum $Kv{\cdot}T$ depends upon the number of lamps operating. However, the total $Kv{\cdot}T$ is not directly proportional to the number of lamps, indicating that the harmonic components from the individual lamps are not exactly equal or exactly in phase. The curve in figure 4 gives the total $Kv{\cdot}T$ observed on circuits supplying various numbers of lamps.

5. Since the influence varies from a maximum at the supply end to a minimum at the far end of the lighting circuit, the direction of feed may have an important effect on the magnitude of the induction. This will be most noticeable for nonuniform exposures—for example, where a section of joint use exists at one end of an exposure, the remainder of which is at highway separation. If in such a situation a choice were available as to the direction of feed, the location of the constant-current transformer at the end of the circuit remote from the joint-use exposure section would result in the lower magnitude of induction.

Figure 10. Capacitance values

CAPACITANCE VALUES
MICRO-MICROFARADS PER KILOFOOT

DIRECT CAPACITANCES JOINT USE (SEE FIG. 11A)

	No. Power Cond. →	4-Foot Separation: 1	2		4				6-Foot Separation: 1	2		4			
		A	A		A				A	A		A			
10 Wire Telephone Line	B	—	764	B	654	B			—	828	B	685	B		
	C	—	—	—	327	704	C		—	—	—	359	740	C	
	D	—	—	—	158	213	378	D	—	—	—	195	252	425	D
	1	91	67	86	43	47	67	159	92	65	83	42	46	62	132
	2	81	58	78	38	43	62	144	80	56	73	37	42	56	113
	3	83	59	83	38	45	67	152	80	56	74	37	42	57	106
	4	97	69	99	44	54	82	162	92	65	84	45	51	67	113
	5	118	83	118	56	69	103	167	108	76	97	54	59	75	111
	6	178	130	166	103	115	136	117	133	98	93	77	74	81	86
	7	177	134	154	111	111	115	83	126	93	101	76	70	72	67
	8	191	149	145	131	110	97	64	131	101	98	84	70	66	56
	9	213	173	141	157	110	87	56	169	127	115	115	87	76	60
	10	258	212	154	197	123	90	60	198	156	129	142	94	81	61
	Σ	1490	1900	1990	2060	2400	2310	1910	1210	1720	1790	1960	2310	2210	1770
	G	920	726	670	616	461	469	634	1120	850	809	698	535	552	735
	TOTAL	2410	2620	2660	2680	2870	2780	2550	2320	2570	2590	2660	2860	2760	2520
20 Wire Telephone Line	B	—	763		654				—	827		690			
	C	—	—	—	325	704			—	—	—	360	730		
	D	—	—	—	155	211	375		—	—	—	189	245	424	
	1	65	47	64	30	35	51	131	71	50	67	32	35	50	110
	2	56	41	57	25	30	46	118	61	43	58	27	31	44	92
	3	62	44	65	27	34	54	132	61	43	58	27	32	45	86
	4	75	52	79	32	43	69	143	73	51	68	34	40	55	94
	5	90	63	96	41	55	85	144	82	58	76	39	45	59	87
	6	147	107	142	83	101	119	98	104	74	87	57	57	64	66
	7	148	111	131	92	97	100	68	105	76	82	61	57	60	53
	8	159	124	121	110	94	80	48	106	78	78	67	56	53	43
	9	178	147	117	134	92	71	41	141	110	97	99	73	64	46
	10	219	185	130	171	105	74	44	170	136	109	121	82	68	48
	11	33	25	32	17	18	25	56	37	26	33	18	19	25	53
	12	25	18	23	13	14	19	40	26	19	24	13	14	18	36
	13	23	17	22	12	13	18	35	24	17	21	12	12	16	28
	14	27	19	26	14	16	22	36	25	18	22	13	14	17	27
	15	38	27	36	20	22	30	44	31	22	26	16	17	21	29
	16	49	37	42	30	29	33	35	39	28	31	23	21	24	26
	17	43	33	34	28	24	26	24	35	26	26	22	19	19	20
	18	49	39	35	34	26	24	19	39	30	27	24	20	18	17
	19	63	52	41	46	31	26	19	52	41	34	35	25	22	18
	20	95	79	56	72	44	34	24	80	64	50	56	37	31	24
	Σ	1650	2040	2120	2160	2490	2420	2040	1380	1840	1910	2040	2330	2290	1870
	G	780	626	560	532	398	400	536	974	741	693	610	460	471	633
	TOTAL	2430	2670	2680	2700	2900	2820	2570	2350	2590	2610	2650	2790	2770	2470

LONGITUDINAL–CIRCUIT COUPLING CAPACITANCES

HIGHWAY SEPARATION * (SEE FIG. 11B)

TEL. PAIR	BALANCED INDUCTION	RESIDUAL INDUCTION **
1–2	4.7	68
3–4	2.3	33
5–6	1.8	26
7–8	1.5	21
9–10	1.75	25
11–12	3.0	44
13–14	.9	6.1
15–16	.47	6.8
17–18	.42	6.1
19–20	.76	11
21–22	2.7	37
23–24	.68	9.8
25–26	.36	5.1
27–28	.3	4.2
29–30	.53	7.6
31–32	3.2	46
33–34	1.25	18
35–36	.76	11
37–38	.57	8.1
39–40	.68	9.8
Σ	28.	400

* FOR 30-FT. SEPARATION BETWEEN NEAREST TELEPHONE AND LIGHTING CIRCUIT WIRES. SEE FIG. 12 FOR CORRECTION FACTOR TO BE USED IN OBTAINING VALUES FOR DIFFERENT SEPARATIONS.

** FOR A SINGLE-WIRE LIGHTING CIRCUIT MULTIPLY THESE VALUES BY 1.5

6. The influence of a sodium lighting circuit (assumed ungrounded) is a minimum when the two wires of the circuit are kept close together (for example, adjacent pin positions) and when adjacent lamps are connected in opposite sides of the circuit. Figure 6 gives an indication of the relative effects of various lighting circuit arrangements.

7. With the lighting circuit arranged for minimum influence, that is, with no grounds, with the wires closely spaced, and with the lamps staggered, the induction in joint-use exposures is of the order of ten times that occurring at roadway (35-foot) separation.

8. For the lighting circuit arrangement just described (item 7), telephone circuit noise results chiefly from direct induction into the metallic circuit, since the longitudinal circuit induction is relatively small. Consequently, telephone circuit transpositions are effective in reducing this noise.

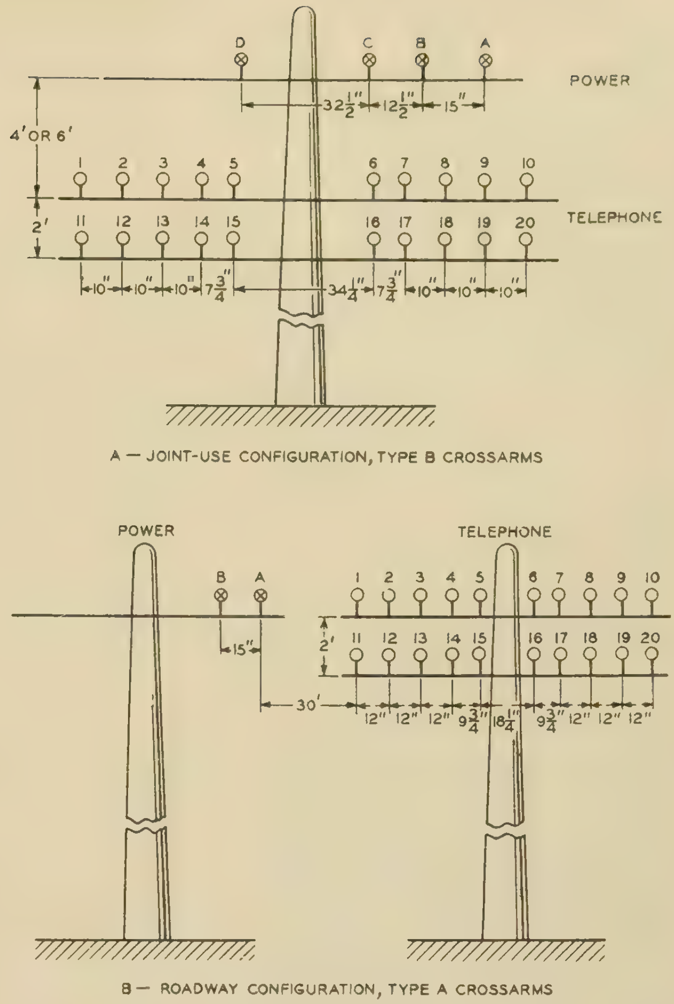
9. It appears that for reasonably uniform exposures at highway separation between open-wire telephone toll lines and a series lighting circuit, the noise induction will not be important if:

(*a*). The telephone lead is transposed according to the exposed line transposition system, or other systems having equal or greater frequency of transposition, and

(*b*). The lighting circuit is not grounded (or is grounded at a balanced point only), the two wires of the circuit occupy adjacent pin positions, and adjacent lamps are connected in opposite sides of the circuit.

10. In situations where only a small number of sodium-vapor lamps are used as, for example, at highway intersections, noise induction is negligible.

Figure 11. Line configuration



Appendix. Method of Estimating Noise

The usual methods of estimating noise[4] in transposed telephone circuits involve an empirical factor to take account of the effectiveness of the telephone circuit transpositions. The value of this factor depends upon the particular exposure and type of transposition arrangement. For reasonably uniform exposures with co-ordinated telephone-circuit transpositions, the factor can be obtained from accumulated experience data with a fair degree of accuracy. Where the telephone circuits are not co-ordinated or where the exposure conditions are quite irregular, the degree of effectiveness of telephone-circuit transpositions may vary over a wide range. The fact that exposures to series sodium lighting circuits may frequently involve both joint use and highway separation has made it necessary to use a method of estimating noise differing somewhat from those generally used.

The formulas employed for noise estimates in joint-use exposures are given in figure 8. The metallic-circuit noise is arrived at by first computing the total noise current to ground in each telephone wire caused by induction from the voltages to ground on each of the lighting-circuit wires. (It should be noted that where the lighting circuit is balanced to ground, the net noise current to ground in a given telephone wire is the *difference* between the induction from the two lighting wires.) This calculation makes use of the direct capacitances[5] between the lighting and telephone circuit conductors. The noise in a given metallic circuit is then obtained from the difference between the total longitudinal currents in the two sides of the circuit. This method is employed rather than a direct computation of noise metallic because of the difficulty of keeping track of the relative signs of the induction in various sections if the latter method is used.

As is indicated by the legend in figure 8, the formulas given are applied to a length of the exposure in which the configuration is uniform. The variation of the influence in such a section is taken into account by using the average value for the section. The usual type of exposure will need to be divided into a number of such sections, points of discontinuity being determined chiefly by the location of the telephone circuit transpositions and major changes in separation between the telephone circuits and the lighting circuit. In the case of toll circuits transposed as phantom groups, the computation of the total noise current in a given wire as, for example, wire 1 (N_1) may involve sections in which wire 1 is on each of the pin positions 1, 2, 3, and 4. The total noise current in wire 1 is then obtained by adding directly the components arising in the various sections.

Figure 9 shows the formulas employed for estimating noise in exposures at highway separation. These are generally similar to those in figure 8, except that the computation is carried out in terms of the balanced and residual components of the lighting circuit voltage instead of the voltage to ground of the individual wires. It will be noted that these formulas make use of "coupling capacitances" rather than the "direct capacitances" employed in the joint-use case.

Values of capacitances required in these formulas are given in figure 10 for the line configurations shown in figure 11. Direct capacitances are given in figure 10 for several joint-use exposure configurations. The "1 power conductor" data apply to a ground-return lighting circuit (or to a metallic circuit, the two sides of which are not on the same pole line) when the lighting circuit conductor is the only one on the power crossarm. If a single-wire lighting circuit were on the crossarm with three power conductors, the capacitance data for that wire would be selected from values for the "4 power conductor" case since the latter take into account the shielding effect of the other power conductors. These same considerations govern the selection of data for the case of a two-wire lighting circuit with both wires on the same pole line. While only a few of the possible number of combinations are covered, it will usually be adequate in a particular case to use the values for the condition most nearly representing the actual situation. In the practical case there are always present various irregularities in exposure conditions which it is not feasible to take into account, and hence noise estimates are necessarily approximate.

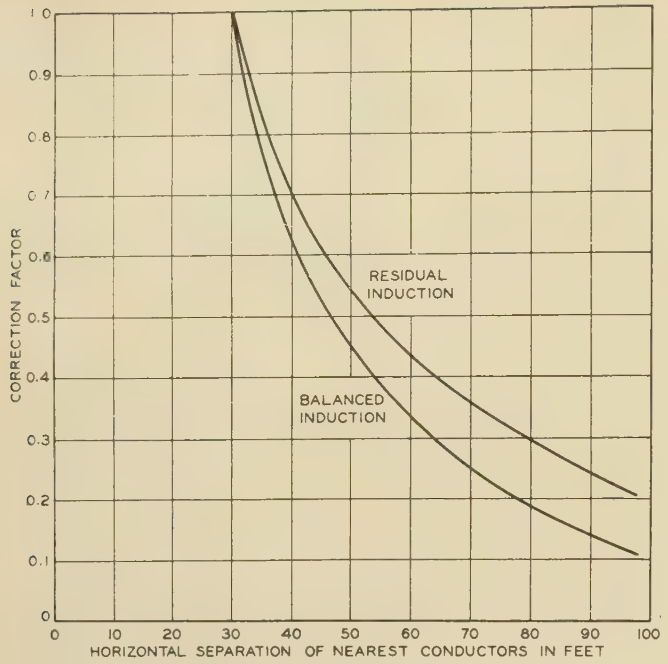


Figure 12. Correction factor for effect of separation for longitudinal - circuit coupling factors of figure 10

The available data, which are quite limited, on the longitudinal circuit coupling capacitance for highway separation exposures are also given in figure 10. These values are for a pair of telephone wires, rather than a single wire. Consequently they make possible estimates of noise in phantom circuits but not in side circuits. Since induction in phantom circuits is generally greater than in side circuits, the noise estimates will show the upper limits of noise to be expected. As an approximation, these capacitance values may be corrected for other values of separation between the lighting circuit and telephone circuits by means of the factors in figure 12. For a single-wire lighting circuit the residual induction values, increased by 50 per cent, should be used. If there are other conductors on the pole line in addition to the lighting circuit, the coupling capacitances will be reduced by the resulting shielding. This reduction may be of the order of 50 per cent.

Combination of Induction
From Various Sections

In view of the apparent differences in phase of the harmonics arising in different lamps, it appears that the best method of combining the noise arising in the highway sections and the joint-use sections is to take the square root of the sum of the squares.

An opportunity to check these methods of estimating noise has been afforded in one field situation. In this case the open-wire telephone and toll circuits were transposed according to the *ABC* system and were exposed for 12 miles at highway separation and for 1.7 miles in joint use. The lighting circuit consisted of three sections, each approximately six miles in length. The two wires of the lighting circuit were located on adjacent pins (15-inch spacing) and a staggered arrangement of lamps was employed. The average contribution of the lighting circuits to the telephone circuit noise was estimated as 100 noise units for side circuits and 250 noise units for phantom circuits. The corresponding measured values were 150 noise units and 350 noise units.

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Discussion

C. W. Frick (General Electric Company, Schenectady, N. Y.): Sodium lighting is a development which utilizes the high efficiency and other desirable characteristics of gaseous-discharge lamps. It does not displace incandescent lighting since each has its own field. In common with other devices in which conduction takes place through ionized gas, either the voltage or the current is distorted, in this case the voltage. The series arrangement does not lend itself to the application of devices for wave-shape improvement.

From the co-ordination standpoint there are three types of situations: first, where the number of lamps is small and the distorted voltage has negligible effect; second, where either the lighting circuit or the paralleling telephone circuits are in metallic-sheathed cable so that shielding practically eliminates the effect of distorted voltage; third, where fairly long parallels exist between open-wire circuits and the distorted voltage may cause noise induction under some conditions.

In the early installations the situations were either of the first or of the second type, and this still applies to a majority of the installations. A good example is the illumination of a traffic circle, an intersection, or a grade crossing. When the utility of this type of lighting for highways had been demonstrated situations of the third type began to appear. It is worthy of note that co-operative studies were undertaken before any of these situations actually developed. The co-ordinative measures described in the paper resulted from these studies. If installations such as the one 16 miles long referred to by the authors had been planned without any thought of co-ordination, the pin position nearest the road might have been chosen for the lighting circuit and all the lamps connected in that wire. The other wire might then have been put in a similar position on the opposite side of the crossarm. The studies showed, however, that such an arrangement would not have given the best results. Actually, arrangements have been chosen which were recommended for minimizing the inductive effects. Thus the lighting circuits have been planned with wires on adjacent pins and the lamps alternated between wires, as recommended in the paper.

When an installation is contemplated where conditions of the third type exist, the data in the paper should be carefully considered from the point of view of both the lighting circuit and the telephone circuit. For roadway separation the problem is practically solved when the recommendations are followed. Up to the present time there has been only a limited amount of experience with these open-wire circuits at joint-use separation.

At the present time there are over 6,000 sodium luminaires in service and we know of no reports of telephone interference from this cause.

P. W. Blye: As mentioned by Mr. Frick in his discussion, it is important, where a parallel is to be created between a series sodium lighting circuit and an open-wire telephone line, that the inductive co-ordination aspects be considered in planning the arrangement of the lighting circuit. A co-operative study of the situation by the power and telephone companies involved, including an estimate of the noise induction, will indicate whether or not special co-ordinative measures will be necessary either in the lighting circuit design or in the telephone line. In cases where special measures are necessary their installation in advance of the energization of the lighting circuit will generally be found not only most economical but also most desirable from the standpoint of avoiding inconvenience to either utility. The present paper has been arranged to provide the technical data required in the co-operative consideration of such problems.

Electromagnetic Horn Design

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Synopsis: The principles of designing electromagnetic horn "antennas" to obtain beams of specified angular spread, smoothness of contour and power gain are disclosed. Quantitative curves are given from which the design of sectoral and pyramidal horns may be readily made.

THIS PAPER deals with the transmission and reception of ultrahigh-frequency electromagnetic waves by flared horns of metal. Several earlier papers (see bibliography) have described the operation and some of the applications of these new "antennas" and have reported fundamental research of both experimental and theoretical nature. In this paper, some of the more important principles of design will be discussed. Numerical data based on theoretical considerations will be presented that permit the specification of horn dimensions for given radiation performance.

General Considerations

The electromagnetic horn comprises a formed sheet of conducting material flared from a "throat" or small end to a "mouth" or large end. In applications to transmitting, electromagnetic energy delivered to the throat propagates through the interior of the horn to the mouth as "horn waves." At the mouth, substantially all of this energy is radiated as free-space or ordinary radio waves. In applications to receiving, a similar but reverse process occurs. It may be shown by the general theorem of reciprocity for electromagnetic systems, that a given horn will have characteristics as a receiver similar to those that it has as a transmitter, hence the material to follow applies to both applications.

Although a wide variety of shapes is possible, a shape having a rectangular cross section perpendicular to the central axis of the horn is preferable, because it is capable of producing a linearly polarized wave. The rectangular shape also permits an independent control of the width of the radiated beam in the horizontal and vertical planes. Horns having straight sides in the longitudinal cross section have been most used, because of the economy and ease of construction and because thorough analysis of this structure has been made. Pyramidal and the sectoral horns, having both sides flared and having two opposite sides flared and the other two opposite sides parallel, respectively, are the most important examples. The analysis of this paper applies directly to sectoral horns and indirectly to those of pyramidal shape.

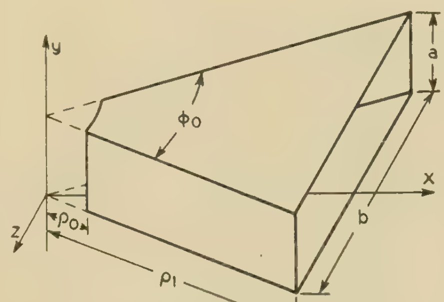

Figure 1. Sketch of sectoral horn showing dimensions

The material from which the horn is made may be any of the highly conducting metals. Galvanized iron sheeting and thin electrolytic copper foil cemented to plywood have both proved satisfactory. Screen or other semiopen construction may also be employed. Dielectric supports and insulators are not required in any region of intense field, consequently dielectric losses are low. For protection from weather, however, the mouth may be tightly covered with a dielectric material, such as silk, fabric, or plywood.

Two general methods of exciting the horn are available; namely (1) hollow-pipe feed, and (2) direct excitation. In the first case a hollow-pipe transmission line is connected to the throat, and in the second case an exciting rod or other radiating means, such as a vacuum tube, is disposed directly in the throat. The first method is mainly useful for wave lengths less than about 20 centimeters, but the second method is applicable to longer waves. Experiment has shown that substantially the same radiation patterns may be produced with either arrangement.

Figure 12 shows a large pyramidal electromagnetic horn that was designed in accordance with the principles outlined in this paper. It is constructed of plywood and copper foil. It is designed for optimum sharpness of beam in the vertical plane when operated at a wave length of 50 centimeters. This horn was constructed in connection with a research program on the instrument landing of airplanes conducted at Massachusetts Institute of Technology for the Civil Aeronautics Authority.

The sketch of figure 1 shows a sectoral horn and serves to define the following quantities: flare angle ϕ_0, horizontal aperture b/λ, vertical aperture a/λ, radial length ρ_1/λ, and the cut-off length ρ_0/λ, where λ is the wave length. The same unit of length is used for all dimensions and for the wave length.

Propagation within the horn may take place by means of any of several different types of horn waves, or by a combination of them. Which of the types is present depends on the configuration of the exciting system at the throat or of the waves delivered there by a hollow-pipe transmission line, and also on the flare angle ϕ_0 and the cut-off length ρ_0/λ. Expressions for the field configurations and transmission properties of these waves may be obtained by solving Maxwell's equations in cylindrical co-ordinates and satisfying the boundary conditions on the surfaces of the horn.[6] In general, two distinct groups of waves result; namely, E waves, having no radial component of magnetic intensity, and H waves, having no radial component of electric intensity.* For most applications of the horn, the H waves are employed, particularly the two waves of lowest order, $H_{0,1}$ and $H_{1,0}$. The reason for this choice is found in the fact that the configuration of the field of these waves inside the horn is such as to produce single-lobe beams of linear polarization in the radiated wave.

Sketches of the field configurations of the $H_{0,1}$ and the $H_{1,0}$ waves are reproduced in figure 2. The electric intensity in the $H_{0,1}$ wave is everywhere parallel to the y axis, is uniform in intensity in this direction but has a half-sinusoid distribution in intensity along an arc between the two flared sides. The magnetic lines lie in planes perpendicular to the y axis. This wave may be excited by a current-carrying rod in the throat disposed par-

Paper number 39-45, recommended by the AIEE committee on communication, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 5, 1938; made available for preprinting December 30, 1938.

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6. For all numbered references, see list at end of paper.

* Two subscripts, m and n, are required to define a particular wave. These subscripts give the number of half sinusoids in the field distribution between the two parallel sides and between the two flared sides, respectively.

allel to the y axis, or by feeding an $H_{0,1}$ wave into the throat from a rectangular hollow pipe. In the $H_{1,0}$ wave, the electric lines of force lie along arcs between the two flared sides; they have a uniform distribution along the arc, but a half-sinusoid distribution in the y direction. The magnetic lines lie in planes passing through the y axis. This second type of wave may be excited by a current-carrying rod disposed centrally in the throat parallel to the x, z plane and along an arc about the vertex, or by feeding an $H_{1,0}$ wave into the throat from a rectangular hollow pipe. Both waves have constant phase on cylindrical surfaces about the y axis.

Design Considerations

In the design of electromagnetic horn radiators, two aspects of the horn are of fundamental importance. The first of these has to do with the excitation within the horn of the desired type of wave to the exclusion of waves of other types. In addition to the provision of an appropriate disposition of the exciting rod or rods, it is also necessary to make the size of the throat and the radial length of the horn of such values that the desired wave only will be produced for radiation at the mouth. The determination of these values will be discussed in the section entitled "Throat Design." The second important aspect of design concerns the radiation into space of a beam that meets

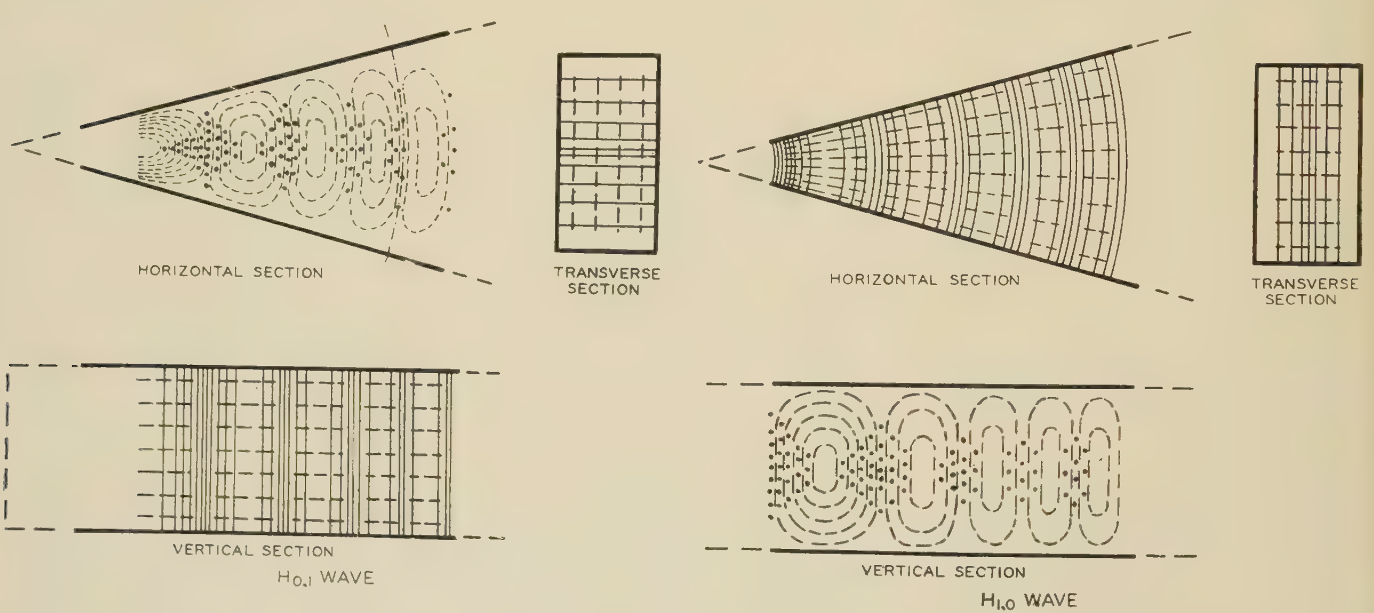

Figure 2. Field configurations for $H_{0,1}$ and $H_{1,0}$ waves in a horn

Solid lines represent electric lines of force and dotted lines represent magnetic lines of force

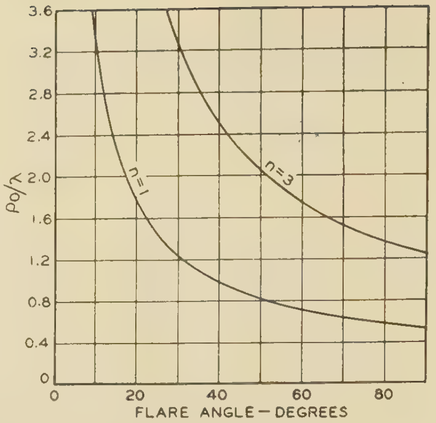

Figure 3. Throat design curves

Optimum cut-off length ρ_0/λ for the $H_{0,1}$ wave ($n = 1$) and the radial extent of the high attenuation region for the $H_{0,3}$ wave ($n = 3$)

the given requirements as to smoothness, sharpness, and concentration of the radiant energy in one direction. Two of the dimensions of the horn, in the plane in which these requirements are given, must be made to comply with definite values. For example, the flare angle and the length, or the flare angle and the horizontal aperture, etc., must be appropriately designed. These matters are considered at length in the remaining sections of this paper.

Naturally, a considerable background of mathematical analysis antecedes the quantitative curves and the discussions of this paper. This analysis, which is in part published elsewhere,[3,6] has been omitted from this paper in order to emphasize the design of actual horns for specific applications. Experimental measurements have been likewise omitted, although on hand in abundance. Some

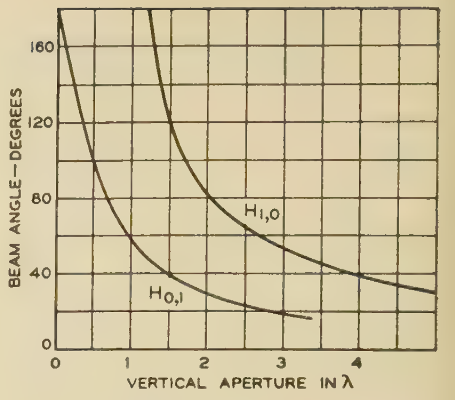

Figure 4. Beam angles in the vertical plane for $H_{0,1}$ and $H_{1,0}$ waves

experimental results have already been published.[3,5]

Throat Design

In the vicinity of the exciting rod, a plurality of the different types of horn waves will be generated. In many applications a single wave, say $H_{0,1}$, should obtain near the mouth, as the radiation pattern will be distorted by the presence of other waves. A single wave near the mouth can be realized by appropriate design of the throat. The magnitude of the attentuation of each wave is relatively large near the apex but is progressively smaller at greater radial distances. The radial distance to which the region of relatively high attenuation extends is greater for waves of higher order than for waves of lower order. Consequently, in a horn of given flare angle, a particular value for the cut-off length ρ_0 can be given that permits the $H_{0,1}$ wave to reach the mouth substantially unattenuated but which affords almost complete at-

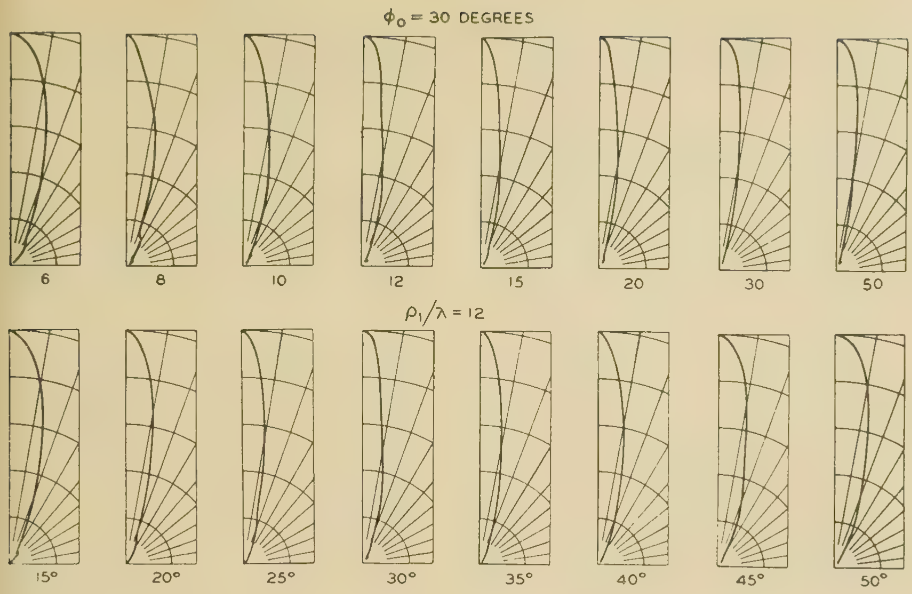

Figure 5. Two typical series of horizontal radiation patterns for the $H_{0,1}$ wave

Upper series for constant flare angle of 30 degrees and variable radial length. Lower series for constant radial length of 12 and variable flare angle

tenuation or filtration of higher order waves. This value of ρ_0 will be referred to as the "optimum cut-off length." Horns for the production of single-lobe smooth beams should have their cut-off lengths not too different from this optimum value. Figure 3 shows graphically the relation between the optimum cut-off length and the flare angle for the $H_{0,1}$ wave. For comparison, the radial extent of the high-attenuation region for the $H_{0,3}$ wave is given by the curve $n = 3$. The vertical dimension a affects neither the transmission characteristics of the $H_{0,1}$ waves nor the filtration of higher order waves except when $a > > \lambda$.

When radiation by means of the $H_{1,0}$ wave is desired, the $H_{3,0}$, $H_{5,0}$, . . . waves must be eliminated or filtered, which may be achieved by adjusting the dimension a. The $H_{1,0}$ wave is able to travel freely in the radial direction when $a > \pi/2$, but the $H_{3,0}$ wave requires $a > (3\lambda)/2$ for free transmission, etc. Therefore, to eliminate the higher order waves, the dimension a must be slightly greater than one-half wave length but less than three-halves wave lengths. The $H_{2,0}$ and $H_{4,0}$ waves can be prevented by constructing the exciting system with even symmetry about the plane $y = a/2$ equidistant from the two parallel sides of the horn.

Special cases may arise wherein several wave-types may be used simultaneously. It will be assumed in the remainder of this paper, however, that the construction of throat and exciting means is such that either an $H_{0,1}$ or an $H_{1,0}$ wave alone exists in the horn.

Radiation Characteristics

When the horn waves reach the mouth, they become free from the guiding surfaces and spread out as radiant energy. The relative amplitude of the electric field intensity on a sphere of fixed radius large compared to wave length and aperture comprises the space radiation pattern. The radiation pattern along the intersection of this sphere and the x, y plane will be referred to as the vertical pattern and that along the x, z plane as the horizontal pattern. The discussions of this paper are confined to these two plane radiation characteristics.

In evaluating the effectiveness of a given horn to produce a directed beam of radiation, recourse to the following knowledge is useful: (1) the detailed shape of the radiation pattern, such as the presence and relative amplitudes of secondary lobes; (2) the angular width of the beam, or the "beam angle," defined here as twice the angle measured from the forward axis of the radiation pattern to the nearest radial line in this pattern along which the magnitude of the electric field intensity is ten per cent of its value on this axis; and (3) the relative power gain, defined as the ratio of the power radiated from a dipole to that radiated from the horn to produce, in each case, the same magnitude of electric field intensity at a fixed remote point on the x axis.

The radiation patterns were computed by means of Huygens' principle from the distribution of the Hertzian vector at the mouth.[3] This distribution was assumed to be the same as that which would exist at the plane of the mouth were the horn infinitely long: experiments have justified this assumption for most practical cases. The radiation patterns were plotted both in rectangular and in polar co-ordinates, and the beam angle was measured from the rectangular plots. The power radiated by the horn was obtained by integrating the Poynting vector over the mouth with the field at the mouth adjusted to give unity power density at a fixed distance r on the x axis from the origin. The power radiated by the dipole for the same effect is $(8\pi r^2)/3$. Al-

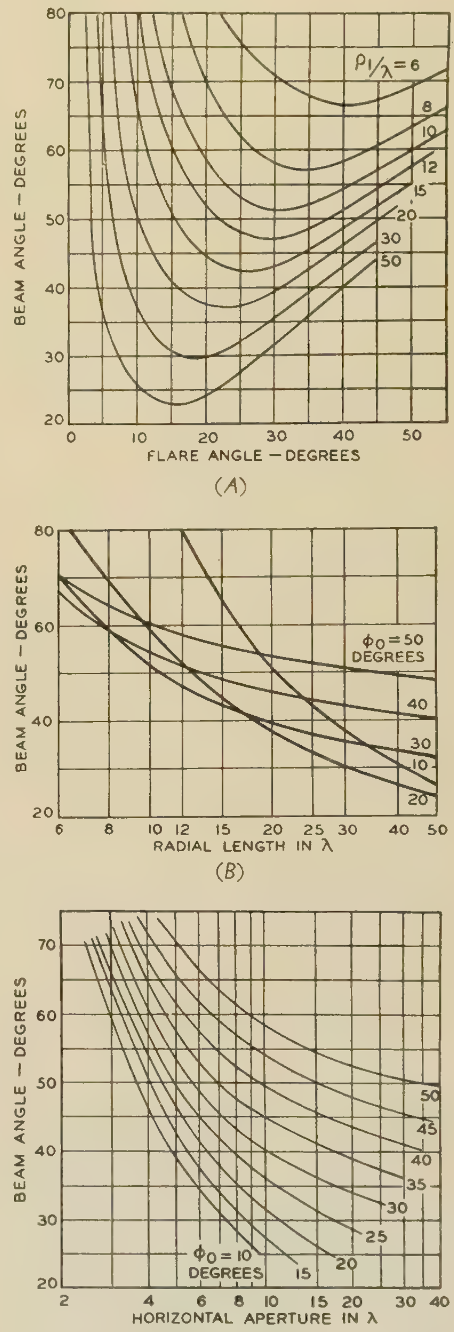

Figure 6. Beam angle in horizontal pattern for $H_{0,1}$ wave versus (A) flare angle, (B) radial length, and (C) horizontal aperture

though the power gain obtained in this way does not include copper losses in either horn or dipole, it is believed sufficiently accurate for most purposes.[6]

In the course of this research over a hundred radiation patterns were calculated for a wide range of horn parameters, plotted, and analyzed. Only a few of these patterns are reproduced here, but they show the same general shapes and trends possessed by all of the curves. A number of experimentally measured curves are on hand,[5] all of which agree satisfactorily with the calculated ones. Thus, the composite curves of beam angle, power gain, etc. are based on a considerable and, we believe, adequate amount of data.

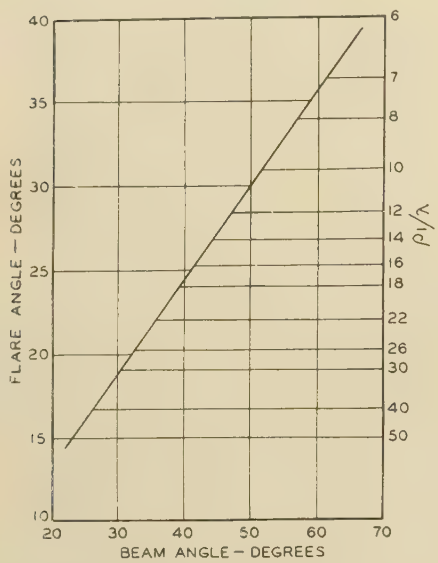

Figure 7. Optimum design curve for $H_{0,1}$ wave on beam-angle basis, giving shortest radial lengths and corresponding flare angles for a specified horizontal beam angle

The vertical patterns (in the x, y plane) have the same shapes as those of a rectangular hollow-pipe radiator. The explanation lies in the fact that the distributions of the horn waves in the y direction are similar to the distributions of the corresponding hollow-pipe waves in this direction. Patterns have been given elsewhere[3] and will not be reproduced here. The shape of the patterns comprises a principal lobe centered on the x axis and secondary lobes of relatively small amplitude. The sharpness of the principal lobe depends mainly on the vertical aperture a/λ. Figure 4 shows curves of beam angle versus vertical aperture for both $H_{0,1}$ and $H_{1,0}$ waves.

$H_{0,1}$ Wave

Two typical series of horizontal patterns for the $H_{0,1}$ waves are shown in figure 5. Only half of the beam is reproduced, but the opposite half is a mirror image of that which is given. Calculations for the back 180-degree sector cannot be made. The upper series for $\phi_0 = 30$ degrees shows the variation with radial length ρ_1/λ, and the lower series for ρ_1/λ shows the effect of flare angle ϕ_0. A glance at these patterns reveals that (1) for constant flare angle the sharpness of the beam is improved by increasing the length of the horn, up to a certain length, beyond which very little improvement occurs, and (2) for constant length there is a value of flare angle for which the beam angle is maximum. Although the radiation patterns show slight irregularities, secondary lobes are substantially absent from all of them. This fact, of considerable significance to some applications, is attributable to the half-sinusoid distribution of electric intensity at the mouth.

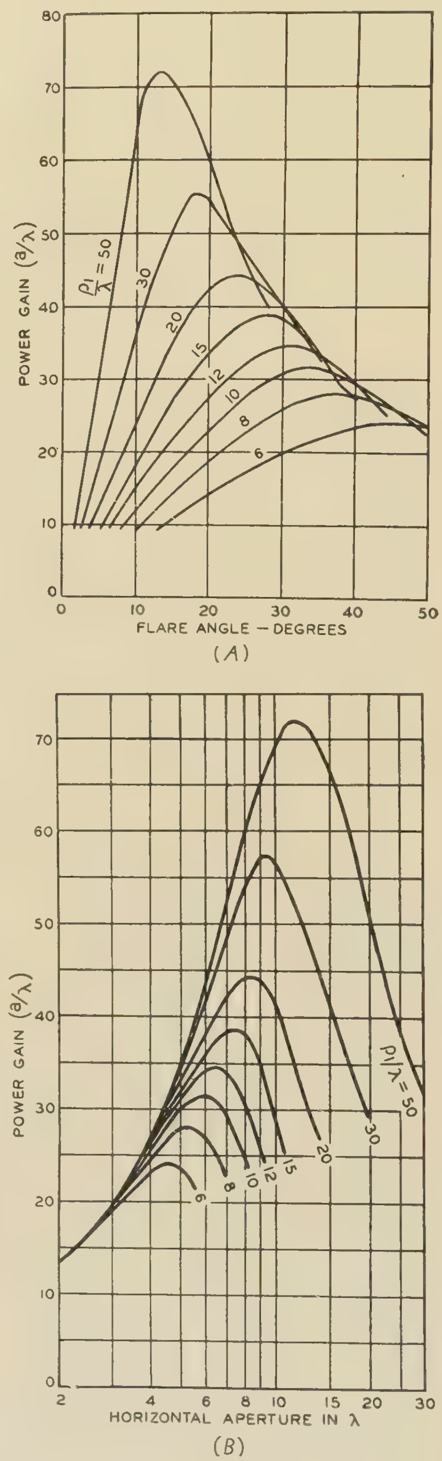

Figure 8. Power gain for $H_{0,1}$ wave versus (A) flare angle and (B) horizontal aperture

The values of power gain as read from the ordinate scale must be multiplied by the vertical aperture a/λ of the horn

Curves are shown in figures 6*A*, *B*, and *C* relating the beam angle with the several design parameters. The trends (1) and (2) mentioned in the preceding paragraph may be traced in each of these curves. In particular, each curve in *A* has a minimum, and a line connecting these minima would be approximately a straight line through the origin. Such a straight line defines the shortest horns that may be employed to produce a beam of specified angle. A corresponding envelope line could be drawn in *B*.

Optimum conditions may be expressed numerically by plotting associated values of optimum flare angle and length versus the beam angle, as has been done in figure 7. This important design curve permits the ready specification of optimum horn dimensions for a beam of given angle.

Curves showing the variation of power gain with flare angle for a number of constant radial lengths are reproduced in figure 8*A*. Here, too, one finds that for a horn of any given length there is an optimum flare angle. In this case, it provides maximum power gain. The

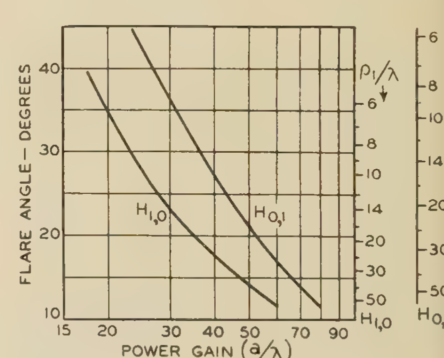

Figure 9. Optimum design curve for $H_{0,1}$ wave and $H_{1,0}$ wave on a power-gain basis, giving shortest radial lengths and corresponding flare angles for a specified power gain

The values of the power gain as read from the abscissa scale must be multiplied by the vertical aperture a/λ of the horn

values of the maxima occur at smaller flare angles for increasing lengths of horn. The magnitude of the maximum power gain increases with increasing length. Figure 8*B* shows the variation of power gain with horizontal aperture. If the abscissas are multiplied by a/λ, this curve will show the variation of power gain with the transverse area of the mouth. Clearly, the gain is not a simple function of the mouth area.

The peaks of the curves in figure 8 provide optimum design conditions on a power gain basis. These optimum values of flare angle and length that provide maximum power gain are plotted in figure 9. This curve, as well as the accompanying one for the $H_{1,0}$ wave, allows the specification of optimum horn dimensions for a given power gain and supplements the curve of figure 7.

The absolute magnitude of the power gain is plotted for a vertical aperture a/λ = unity, but one should note that the power gain is directly proportional to the vertical aperture. Consequently, the values of power gain given in the curves are to be multiplied by the value of a/λ of the horn in question. Clearly, quite enormous power gains may be obtained. For example, for a horn of length $\rho_1/\lambda = 50$ having an optimum flare angle of 14° and a vertical aperture of a/λ of 10, the power gain is 720. A horn of these dimensions is entirely feasible and of moderate size at wave lengths of, say, ten centimeters. The power gain of the horn shown in the accompanying photograph is calculated to be 50.

$H_{1,0}$ Wave

Two representative series of horizontal radiation patterns (in the x,z plane) are shown in figure 10. The upper series is for horns of a constant flare angle of 35 degrees and the lower series for horns of a constant radial length of 15. The same general trends are found here that were noticed in figure 5 for the $H_{0,1}$ wave. However, the order of magnitude of the secondary peaks in the patterns is considerably greater with $H_{1,0}$ waves. For horns of equal length, increasing the flare angle from a small value at first will sharpen the principal part of the beam. It also increases the magnitude of the secondary peaks. For sufficiently large flare angles, these secondary peaks become larger than the principal beam. As a consequence, the beam becomes broader as the flare angle is increased. For horns of constant flare angle the tendency is also observed for the beam to broaden as the length of the horn is increased. However, for sufficiently great lengths, the width of the beam is substantially equal in magnitude to the flare angle.

The explanation of the exaggerated secondary peaks in the pattern of the $H_{1,0}$ wave lies in the uniform distribution of the field across the mouth in the horizontal direction and the abrupt discontinuity at the edges. The irregular shape of the horizontal patterns makes it impractical to define a beam angle and reference must be had to the actual radiation patterns.

Power gains for the $H_{1,0}$ wave are given by the curves of figure 11*A* and figure 11*B*. The general behavior of these curves is similar to those of the curves for the $H_{0,1}$ wave. The small oscillation of power gain at large flare angles is associated with the shift of the energy from principal lobe to secondary lobe, as described above. The power gain is a

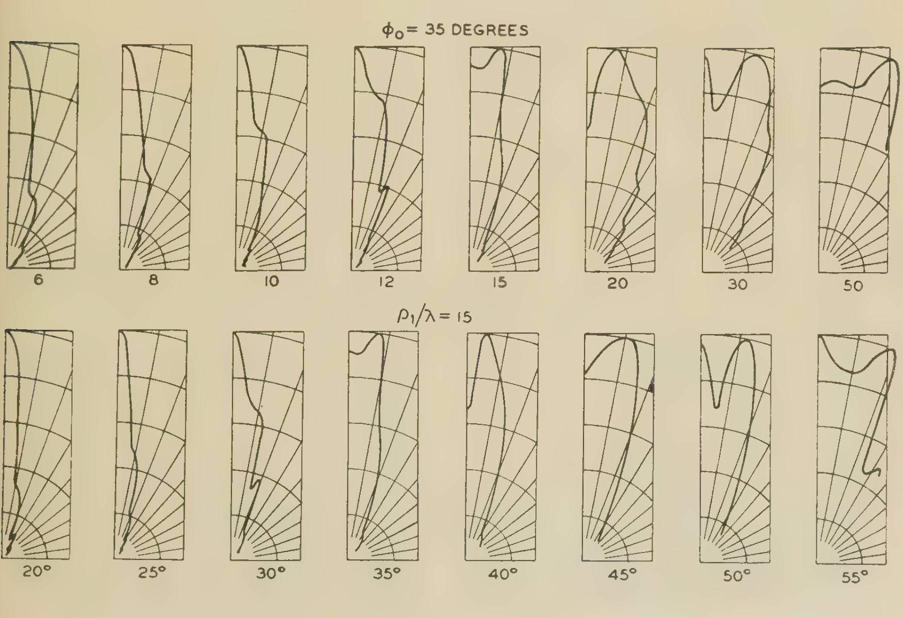

Figure 10. Two typical series of horizontal radiation patterns for the $H_{1,0}$ wave

Upper series for constant flare angle of 35 degrees and variable radial length. Lower series for constant radial length of 15 and variable flare angle

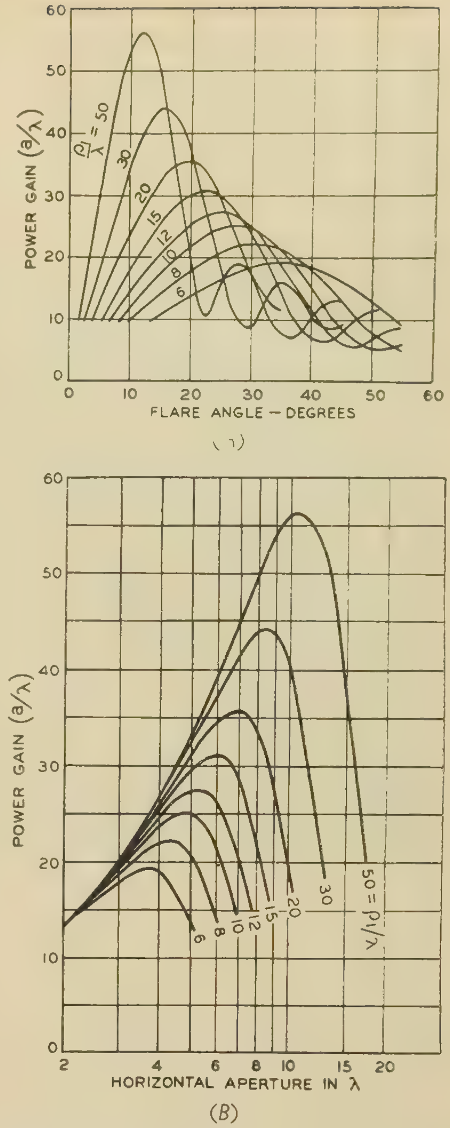

Figure 11. Power gain for $H_{1,0}$ wave versus (A) flare angle and (B) horizontal aperture

The values of power gain as read from the ordinate scale must be multiplied by the vertical aperture a/λ of the horn

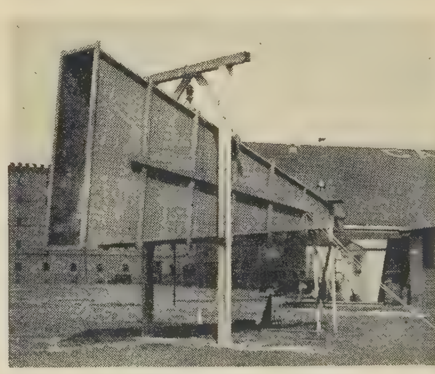

Figure 12

linear function of the vertical aperture a/λ for the $H_{1,0}$ wave also.

Optimum design for $H_{1,0}$ waves on a power gain basis is shown by the indicated curve of figure 9. Clearly, the power gain can be indefinitely increased by lengthening the horn and appropriately reducing the flare.

Concluding Remarks

Comparing the operation of the sectoral horn with the $H_{0,1}$ and the $H_{1,0}$ waves we arrive at the following conclusions. The $H_{0,1}$ wave gives a clean-cut beam comprising a principal lobe without appreciable secondary lobes, and the beam angle can be well controlled. The $H_{1,0}$ wave also gives a clean-cut beam for flare angles less than those required for maximum power gain, but an irregular beam for greater angles. On a power gain basis, there appears to be substantially no difference between the two waves.

Horns of pyramidal shape; that is, flaring in both transverse directions, may be designed on an approximate basis by means of the data presented in this paper. We may express the power gain $P_{0,1}'$ of a pyramidal horn operating with an $H_{1,0}$ wave roughly by the expression $P = kP_{0,1}\, a/\lambda$, where k is a numeric less than unity depending on the horizontal flare angle and the radial length. For small flare angles in the vertical plane, k will not differ greatly from unity, but for greater flares it will be substantially less than unity. When using an $H_{0,1}$ type of wave in a pyramidal horn, the vertical flare angle should be kept small if a clean-cut vertical radiation pattern is desired.

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Electric Drives for Supercalenders in Paper Mills

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MEMBER AIEE

A TYPICAL electric drive for a modern 84-inch supercalender and winder consists of a 200-horsepower adjustable-speed driving motor, two 20-horsepower motors for winder drums, a 150-kw variable-voltage motor generator set with 30-kw winder generator and exciter, full magnetic control with large motor-operated rheostat, and an operator's panel. The drive will provide a threading speed of 50 feet per minute and a wide range of many productive calendering speeds to a maximum which may be 2,000 feet per minute. With auxiliary equipment on the winder slitters, and pressure-applying arrangement, the operator has full control of the calender speed, starting and stopping, roll pressures, and winder tensions at a central point.

In the last two decades progress in the development of electric drives for paper-mill supercalenders has resulted in their adaptation to many existing calenders as well as in their selection for new calender installations.

The Supercalender

The supercalender is a machine consisting of a vertical stack of smooth-surfaced rolls, usually alternately steel and composition fiber, either cotton or paper, with means for applying and regulating pressure on the top roll, thereby creating pressures in the order of one-half to one ton per inch of roll face between the surfaces of the several rolls. Power is applied to the shaft of the bottom roll to produce rotation of all the rolls in contact. Machine-made paper or subsequently processed paper such as heavily coated material is run through the successive nips of the supercalender from a roll of paper mounted in an unwinding stand, then rewound. As the paper passes through the calender rolls it receives a high surface finish.

Previous Practice

For many years supercalendering was carried on with the paper traveling not over 500 feet per minute. Machines were provided with belt drives from mill line shafts, and mechanical clutches. Threading of the web of paper through the rolls to get each roll started was accomplished by slipping the clutch, or in some cases by the use of back gearing. Each machine was limited in respect to its productive speed. Higher productive speeds were seen to be desirable to increase calendering operations with such grades of paper as could be properly finished with faster operation, but some grades of paper could not be handled and properly finished at increased speeds. This situation called for means for selecting calendering speeds to suit a variety of conditions. At this time it was recognized that with some grades of paper, slower productive speeds might result in less loss of product and less interruption due to paper tearing and the web breaking, and particularly that means were needed for providing a stable, very slow, calender speed for threading, because considerable material and time were being lost because of paper breaks occurring in the course of getting paper started through the calender.

Essential Requirements

As adjustable-speed drives were applied to supercalenders to make available higher speeds, improvements took place in the design of bearings and methods of lubrication, and in calender roll properties. By 1926, supercalendering was being carried on regularly by some paper mills at speeds in excess of 1,000 feet per minute. The machinery manufacturers have kept apace with developments and today there are calenders which operate at 2,000 feet per minute.

The modern drive is called upon to provide a wide selection of speeds, including the threading requirement which is 50 feet per minute. Acceleration and deceleration must be smooth to prevent pulling apart the web of paper in the calender nips and to avoid uneven pull in

Paper number 39-43, recommended by the AIEE committee on industrial power applications, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 23, 1938; made available for preprinting December 20, 1938.

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the span of paper from the unwinding roll to the calender, and in the span from the calender to the roll winding-up. Quick stopping is essential in order to keep the passage of wrinkled paper between the rolls to a minimum when a paper break does occur because the calender's composition rolls are made of compressed cotton or paper. The smooth surfaces of these rolls become marred ultimately from wrinkled paper in the machine and time taken for regrinding rolls cuts into the productivity of the calenders.

Electric Drive System

In the course of securing adjustable speed by electric drives and working up from the previous 500 feet per minute maximum productive speed, there have been several methods employed. Where direct current was available or provided the shunt-design d-c motors were used and productive speeds obtained by field weakening the motor, with constant armature voltage. Threading speed was obtained by reduced armature voltage using external resistances in series with and shunted across the armature. Variations of this system to serve several drives in the same calendering department involved the use of two or more voltages in the power circuit, for example both 250 and 125 volts for the motor armature with further range of speed adjustment by field weakening available with the armature connected to either voltage, and 35 volts as a source of armature voltage for threading operation eliminating external armature resistance.

Drives were developed to operate on a-c circuits, these being of the wound-rotor induction-motor type. The selections of speeds for productive operation were made with adjustable secondary or rotor resistance. Threading speed was provided, either by use of an auxiliary squirrel-cage induction motor, driving through a suitable reduction and overrunning gear, or by a separate reduced-frequency and alternating-voltage circuit for the wound-rotor motor.

The variable-voltage, or adjustable direct-voltage system was introduced for paper mills not having direct current available, but where the characteristics of the d-c motor were wanted. This involved a motor generator set operating on the mill a-c circuit, with rheostatic field control of the generator to provide adjustable-output direct voltage for the armature circuit of a shunt-design d-c motor used for driving the supercalender. A separate exciter, usually coupled into the motor generator set, furnished a source of constant voltage for the shunt field and control circuits. Threading speed was provided by impressing a low voltage on the motor armature, smooth acceleration and deceleration by adjusting the voltage gradually and smoothly over a wide range between minimum and maximum available armature voltage.

The recent supercalenders installed for operation over wide speed ranges to maximum speed of 1,200 to 2,000 feet per minute are equipped with variable-voltage drives. The use of a low armature voltage to the motor, or of an auxiliary threading motor with suitable reduction gears and an overrunning clutch, are alternate methods of obtaining the threading speed. In many cases, speed adjustment by motor field weakening supplements the range of speed change available by voltage control giving the drive the characteristics of the shunt d-c motor with constant armature voltage over the normal productive range of calendering speeds.

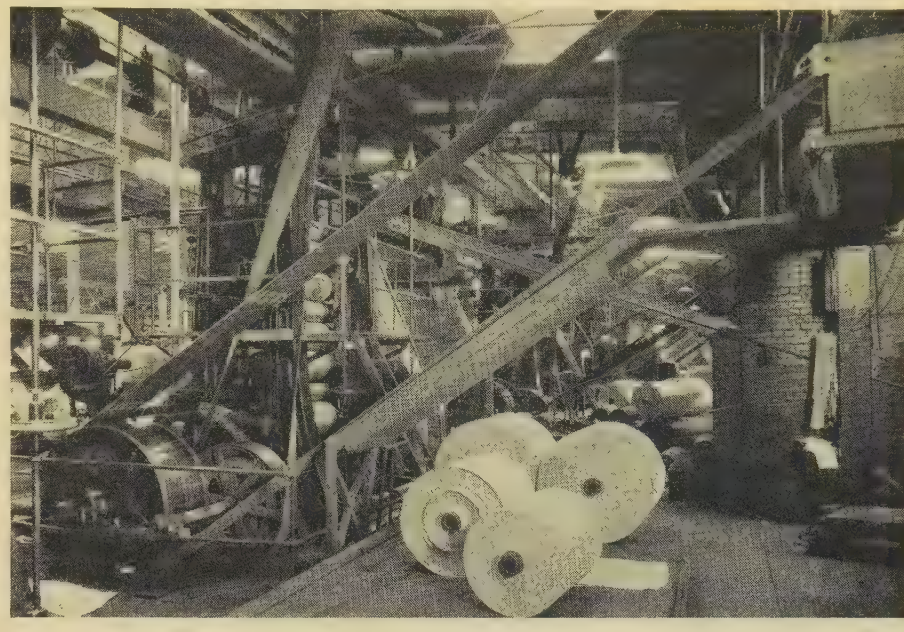

Figure 1. Belt drive for supercalender, commonly used before development of modern electric drives

Load Characteristics

The power requirement of a supercalender is made up of three principal components, (*a*) the work done in overcoming the mechanical frictional losses in the bearings and gearing, (*b*) the work done in the constantly changing deformation taking place in the calender rolls in rotation under contact pressure, and (*c*) the work done on the paper being calendered.

From a large number of observations studied, it appears that for a given set of conditions each component has a constant torque characteristic wherein the horsepower requirement is directly proportional to the linear speed. This pertains over the productive speed range of the calender within the normal capacity of the machine in respect to the pressures applied to the rolls, roll condition, bearing design and lubrication. Tests have shown repeatedly that when the normal conditions for which a machine is designed are exceeded, the load requirement at increased speeds rises abruptly. Recognizing the constant torque characteristic for a given set of conditions, the maximum torque requirement of a supercalender is in some cases that required to drive the calender with heavy roll pressure for a hard finish on a relatively heavy paper normally calendered at some intermediate speed rather than the torque required for calendering the grades of paper which are run through at the maximum linear speed.

The following load information was reported on a modern nine-roll calender, 75 inches wide, equipped with antifriction bearings, the loads expressed in terms of calender roll face and linear speeds:

1. Running without paper and with no pressure applied in addition to weight of rolls themselves.............0.031 horsepower per inch face per 100 feet per minute
2. Running without paper but with pressure applied of 1,780 pounds per inch width in addition to weight of rolls...0.091 horsepower per inch face per 100 feet per minute

3. Running with paper $65^1/_2$ inches wide, basis weight 35 pounds per ream of 500 sheets 24 by 36 inches, with pressure applied of 1,780 pounds per inch width. . 0.132 horsepower per inch face per 100 feet per minute

This is typical in respect to the division of the total load requirement of a supercalender.

Supercalenders made up of alternate steel and composition cotton rolls and used for calendering heavily coated paper require more power than calenders with steel and composition paper rolls used for putting a higher finish on paper as it comes from a paper machine without any further processing, as indicated by a comparison of the following load factors which are averages of observations taken over a long period on several antifriction-bearing calenders:

Cotton and steel rolls—medium weight papers, normal pressures. . . . . .0.140 horsepower per inch face per 100 feet per minute

Paper and steel rolls—medium weight papers, normal pressures. . . . . . . .0.10 horsepower per inch face per 100 feet per minute

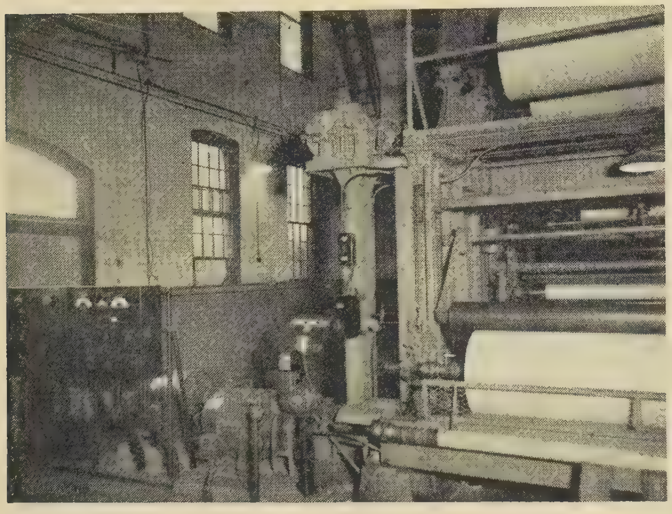

Figure 2. Electric drive for supercalender and two-drum winder having individual motor driving each winder drum

Operator's panels at left mount starting and stopping pushbutton stations, switches for individual slitter motors, instruments indicating calender speed and motor load, and controls for adjusting roll pressure and winder tension

Supercalender Winders

Many supercalenders are equipped with the conventional type of two-arm rotating reels for mounting the unwinding rolls and the cores on which the paper is rewound. A friction brake on the shaft of the unwinding roll produces tension in the web of paper as it enters the calender, and the shaft of the rewinding roll is driven by a belt from a pulley on the shaft of the bottom roll of the calender. As some paper-mill managements found it possible to improve calendering operations with improved equipment and to supply their customers with the paper rolls as they were rewound at the calender without further rewinding to remove imperfect paper, other types of winders were brought into tandem operation with the calenders. These provide for trimming the web of paper and slitting it to desired widths in rewinding.

Surface-Type Winders

There are various designs such as the four-drum winder having an individual motor driving each drum, the two-drum and single-drum winders which wind by the surface principle, the motors for which are controlled automatically to keep in step with the main motor driving the calender and to produce desired tension for handling the sheet properly and securing good finished rolls. Many of these winders are driven by d-c motors with adjustable-voltage control for the overall linear speed range with supplementary or vernier speed control actuated by a counterweighted roll or dancer roll, over which the sheet passes as it travels from the calender to the winder. One electrical manufacturer has supplied a winder system making use of wound-rotor induction transmitter driven by the calender motor and the wound-rotor induction receiver mechanically connected to drive the winder. The primary or stator circuits are commonly excited from the mill a-c circuit and a wound-rotor induction differential is introduced in the rotor circuit. By properly controlling the angular position and rotation of the differential unit, the receiver gains on the transmitter from which it is principally controlled sufficiently to develop the tension desired for proper winding of the roll.

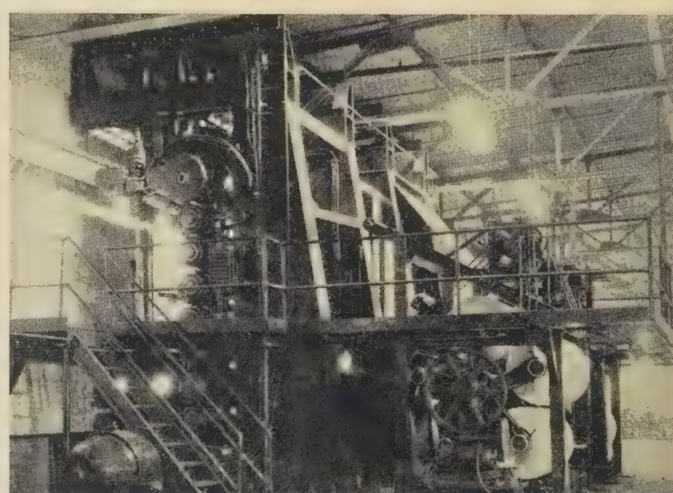

Figure 3. Modern supercalender with variable-voltage d-c driving motor

Magazine reel holds unwinding roll, additional rolls to follow in process, and the electric drive for shaft of core on which paper is rewound

Center-Driven Winders

Some supercalenders are equipped with motor-driven winders of the center or core-shaft drive type. With these the motor driving the winding roll must continually fall off in speed as the roll of paper increases in diameter while no change is taking place in the linear speed of the calender, and simultaneously uniform tension of the sheet must be maintained to produce a good roll. The prevailing practice of using three-inch and four-inch diameter cores for paper rolls for the printing industry has been a limitation preventing the adaptation of tension-controlled motor drives using a field-control d-c motor and automatic regulator working in the field of the motor to maintain constant armature current because the full rolls are 32 inches to 40 inches in diameter. The same limitation has hindered the application of corresponding equipment on unwinding roll for controlling tension automatically

in the sheet entering the calender. Field regulation over a motor or generator speed change of ten to one would be required and this is recognized as a wider range than is available in commercial machines today. The motors which have been applied to shaft or center-driven winders operating in tandem with supercalenders for winding rolls where the ratio of full roll diameter to core diameter is ten to one are of the d-c series design operating in conjunction with specially compounded generators. The regulation of the motor field strength and armature voltage is accomplished inherently in the system by the changing armature current which increases as the roll increases in diameter.

Conclusion

Supercalenders and their winders are outstanding examples of process machines for which motors are called upon to provide unusual flexibility. In addition to transmitting power, the electrical equipment is a production tool vital to the performance of the machine.

Discussion

D. R. Shoults (General Electric Company, Schenectady, N. Y.): We are indebted to Mr. Smith for an interesting résumé of past and present practices in driving supercalenders and their auxiliary equipment.

There have been a number of types of drives used for supercalenders, most of which were reasonably successful when operating at speeds of 500 feet per minute or less, but without question the recent emphasis on high calendering speeds, with a consequent emphasis on ease of acceleration and retardation with readily adjustable operating speeds, has required that the drive used be unusually flexible. The natural trend in such a situation, as Mr. Smith has pointed out, is toward the use of the adjustable-generator-voltage-controlled drives. Such drives lend themselves very well to co-ordinated winder drives or electrical braking systems for unwinding stands.

Another factor which has become of increasing importance as speeds are increased is that of nip pressure between the rolls. In order to obtain equivalent finish on the paper at high operating speeds, the operators have found it necessary to increase the nip pressure considerably above the values common a few years ago. A bottom roll nip pressure of at least three times the pressure resulting from the weight of the rolls themselves is today common practice. Such high pressures give increased roll deformation and naturally influence materially the power requirements of the drive.

Mr. Smith cites a 75-inch-wide supercalender with nine rolls which has a power constant of 0.132 horsepower per inch face per 100 feet per minute. The pressure is 1,700 pounds per inch of width which is assumed to be at the bottom nip. I would like to know whether this calender was equipped with paper or cotton composition rolls. If these rolls are of paper, I believe that that particular requirement is typical, but if they are of cotton, it seems that this value is somewhat low. Mr. Smith subsequently states that *average* observations show that with paper and steel rolls and with *normal* pressure, 0.10 horsepower per inch face at 100 feet per minute is the average requirement. I would like to know what pressures Mr. Smith considered as normal pressures and whether he makes any distinction as to the number of rolls in the stack. The calender stacks have generally 9 to 11 rolls and it is my observation that the power requirement is a direct function of the number of nips in the stack as well as the speed and pressure.

With an up-to-date roller-bearing-equipped calender stack, the four variables are: speed, width, number of nips, and the *average* nip pressure per inch of width. These factors are all essentially linear. Figure 1 of this discussion shows the results of

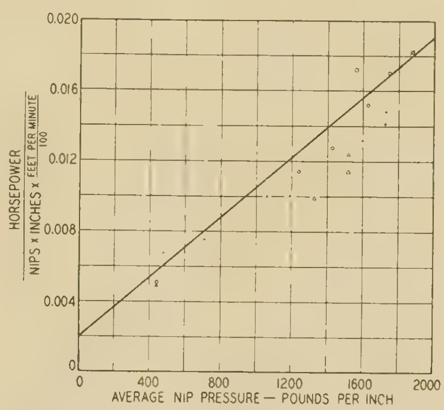

Figure 1. Supercalender power-pressure data; paper rolls

a number of observations correlated as to power requirements as a function of average nip pressure. The average nip pressure is taken, rather than the maximum pressure which occurs at the bottom nip, as being more indicative of the over-all operation of the stack.

These observations, taken on seven different arrangements of supercalenders, show a reasonably close co-ordination when plotted in this manner. If the supercalender which Mr. Smith first cited is assumed to be equipped with paper and steel rolls as were the rest of these, this point corresponds with the others.

Inasmuch as the usual supercalender stack has at least nine rolls, Mr. Smith's constant of 0.1 horsepower per inch face will give on this curve, a constant of 0.0125 horsepower per nip which would be equivalent to an average nip pressure of from 1,200 to 1,600 pounds per inch. The bottom nip pressure, which is more commonly used in expressing pressures, may be from 10 to 20 per cent higher than this.

If an 11-roll stack is assumed, the maximum pressure will be from 950 pounds to 1,350 pounds per inch nip. On this basis, it does not seem that this power constant is adequate for the modern calenders where pressures certainly do approach 2,000 pounds per inch face; furthermore, it appears that power is a very definite function of the number of rolls in the stack and this should be taken account of in general recommendations.

There is one other point worthy of comment and that is that my experience shows no great variation of power with respect to the weight of paper being used, but that the major variation is in the nip pressure. It is true that higher nip pressures may sometimes be used with the heavier grades of paper, but it is more likely that high nip pressures will be used where it is desired to obtain a highly finished paper at high calendering speeds. The finish obtained seems to be a function, not only of nip pressure, but of speed of operation. The paper as it passes through the stack is subjected to a steam bath and the finish depends upon the amount of moisture absorbed. Naturally, the slower the stack runs the more moisture will be absorbed and the higher the finish. A higher nip pressure can offset a reduction in moisture as far as finish is concerned and accordingly, the tendency is to use higher nip pressures to get an equivalent finish when high speeds are required.

Experience with supercalenders operating at speeds from 1,000 to 2,000 feet per minute is not yet extensive enough to indicate clearly definite relation between optimum nip pressure and operating speed. Some operators feel, as Mr. Smith points out, that the greatest pressures may be used at a relatively low speed while others indicate that the maximum pressure may be required to obtain a sufficient finish on any paper calendered at the maximum speed. A more complete investigation of this factor is desirable and in any case, specific knowledge or assumption in this connection is required to choose properly between the constant-field or adjustable-speed motors. Both types of drive operate successfully within their power limits.

The power data presented herein should be supplemented by further observations before any general recommendations are arrived at and it is hoped that interested operators will in the future present such data.

R. H. Smith: The calender cited with a power constant of 0.132 horsepower per inch face per 100 feet per minute is equipped with composition paper rolls. In the subsequent statement, referring to average observation, pressures in the order of 1,200 to 1,600 pounds per inch at the bottom nip were considered normal and a nine-roll calender was assumed.

Supercalendering operations are being carried on at higher pressures between the rolls and calender builders have been progressive in developing means for applying and controlling these higher pressures. The roll makers are called upon to provide composition paper and cotton rolls capable of withstanding these pressures without increased deformation and deterioration.

Anticipation of the load requirement of a calender does require consideration of operating pressures. Means for measuring pressures have been none too reliable, which factor, coupled with the variable of roll hardness and surface condition, seems to prevent establishing rigid rules for absolute power determination at this time.

Cold-Cathode Gas-Filled Tubes as Circuit Elements

S. B. INGRAM
MEMBER AIEE

SINCE the discovery by Hull[1] that oxide-coated cathodes could be used as commercially practical thermionic emitters in gas-filled tubes, these tubes have found extensive use. For high-voltage d-c power supplies two-element mercury-vapor rectifiers have almost entirely replaced rotating machines and the earlier high-vacuum tube rectifiers. The addition of a grid to the gas-filled thermionic rectifier yields the thyratron which is used in regulated rectifiers, for the inversion of direct current to alternating current, and as a sensitive relay in numerous industrial control circuits. The discovery by Slepian and Ludwig[2] of the ignitor principle in initiating the arc spot on a mercury-pool cathode provided a ready means for controlling current flow in tubes with mercury-pool cathodes and the ignitron has now shown itself to have a large field of application.

The present paper will concern itself with gas-filled circuit elements of a third type, cold-cathode tubes. The principles of operation of these devices are not new[3] but their wide application has awaited the development of tubes which will operate in low-voltage circuits of, say, 150 volts or less.[4] Functionally, these tubes have much in common with thyratrons but their current-carrying capacity must remain limited because energy dissipation at the cathode of a glow discharge restricts the current which may be drawn without overheating the surface. However, as control devices in that large class of circuits where milliamperes rather than amperes are required, cold-cathode tubes are capable of performing many of the circuit functions of thyratrons and possess several major advantages over tubes of the hot-cathode type.

General Characteristics of the Glow Discharge

The properties of cold-cathode tubes in which we are interested follow immediately from the well-known characteristics of the self-sustaining glow discharge.

Consider two electrodes in a gas at a reduced pressure. If a low potential is applied between them the few ions which are always present in the gas become multiplied by secondary ionization resulting from impacts of the original electrons and ions with gas atoms and a small current flows through the tube. The gas conduction at this stage is known as the

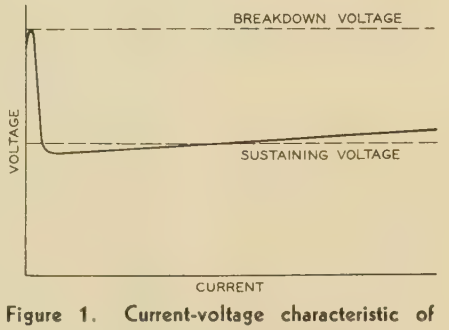

Figure 1. Current-voltage characteristic of typical glow discharge

Townsend discharge and the current which flows is called a Townsend current. It is generally of the order of a microampere or less. As the potential between the electrodes is increased the current increases and at some potential, the breakdown voltage, the gas becomes a good electrical conductor. The voltage across the electrodes now assumes a lower value, which we will call the sustaining voltage, at which it remains practically constant and independent of the current, the magnitude of the current being determined by the external resistance of the circuit. Figure 1 shows schematically the current-voltage characteristic of such a discharge tube.

Cold-Cathode-Tube Structure and Characteristics

Figure 2 shows a photograph of a typical cold-cathode tube. The structure consists of three elements, a cathode, an anode, and a control anode. The cathode is a nickel surface coated with a mixture of barium and strontium produced by reduction from the oxides of these metals by positive-ion bombardment in a glow discharge. The anode is a plain nickel wire shielded from the other elements except for its extreme end by a glass sleeve and separated from them by a spacing of about one-half inch. The control anode is placed close to the cathode. In the particular tube illustrated this element is identical with the cathode and therefore these two elements may be used interchangeably. Such construction, however, is not necessary and in other tubes the control anode may be a wire or small surface near the cathode. The gas filling is a mixture of the rare gases, 99 per cent neon—1 per cent argon at a pressure of about 40 millimeters of mercury. Variations in the constitution of this gas mixture may be used to control the electrical characteristics of the device. In such a three-element cold-cathode tube we have to deal with two conduction paths. That between the cathode and the main anode we will call the main gap and that between the cathode and the control anode the control gap. The nominal characteristics and ratings of this tube are given in table I.

Such a tube has many interesting properties as a circuit element. Basically it may be made to perform three distinct circuit functions, those of a relay, rectifier, and voltage regulator. We shall consider these in turn.

The Cold-Cathode Tube as a Relay

Figure 3 shows a typical cold-cathode-tube relay circuit. The supply potential must be greater than the main gap sustaining voltage but less than the main gap breakdown voltage so that no conduction will normally occur. If now a potential exceeding its breakdown voltage is applied to the control gap the resulting ionization will initiate conduction in the main gap and the anode potential will fall to the main gap sustaining voltage, the current being limited by the external circuit impedance. Conduction will continue until the circuit is opened or the anode voltage maintained below the main gap sustaining voltage for a sufficiently long time for the tube to deionize. The control gap current required to cause breakdown in the main gap we will call the transfer current. In magnitude it does not greatly exceed the Townsend currents flowing just below the breakdown voltage. For the tube described it is in general less than one

Paper number 39-41, recommended by the AIEE subcommittee on electronics, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 21, 1938; made available for preprinting December 22, 1938.

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1. For all numbered references, see list at end of paper.

microampere. The cold-cathode tube is thus a very sensitive relay.

The transfer current is a function of the anode voltage. It must obviously be zero at the main-gap breakdown voltage and approach infinity at the main-gap sustaining voltage. Figures 4*a* and *b* show the transfer-current characteristic of the tube illustrated in figure 2 with two different gas fillings. Neon with a one per cent admixture of argon gives a very low transfer current. One hundred per cent argon gives a much higher transfer current and a higher main-gap breakdown voltage.

In using cold-cathode tubes as relays we must consider not only the amplitude of the signal, which together with the control-gap circuit impedance determines the current available for transfer of the discharge, but also its duration. For the Western Electric 313*C* tube it is found that signals of a duration of 200 microseconds are long enough to give reliable operation even when the amplitude of the signal is small. For signals of greater amplitude a shorter signal duration will suffice.

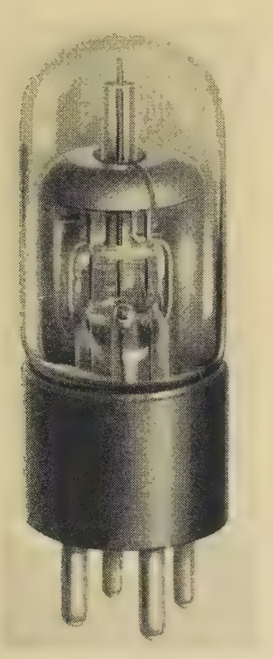


Figure 2. Cold-cathode relay tube

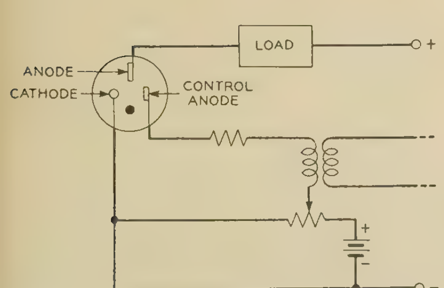
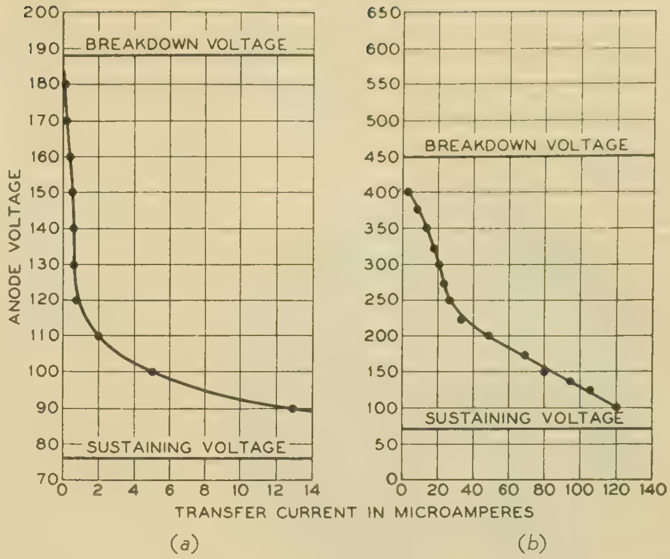


Figure 3. Cold-cathode-tube relay circuit

Figure 4. Transfer current characteristic of cold-cathode relay tube

(*a*)—Gas filling; 99 per cent neon, 1 per cent argon, 40 millimeters

(*b*)—Gas filling; 100 per cent argon, 15 millimeters



Deionization times are of the order of ten milliseconds, somewhat too long for satisfactory operation on 60 cycles. Tubes with shorter deionization times may be made by using gas filling of pure argon but this may be done only at the expense of increasing the transfer current. Thus while such tubes are faster in their operation as relays they are less sensitive.

The stability and reproducibility of tube characteristics are of interest in circuit design. The important characteristics are the control-gap breakdown and sustaining voltages and the main-gap sustaining voltage. The stability of the main-gap breakdown voltage may vary without detrimental effect on the circuits provided it does not fall to the supply voltage. Several years operating experience with tubes of the 313 type have shown that in general the three characteristics mentioned above will remain within plus or minus five volts of their initial values over several thousand hours of operating life if the current ratings are not exceeded. Deterioration of the tubes or shifts in characteristics on standing are negligible. Variations in initial characteristics will depend upon the degree of manufacturing control and, in general, will not exceed plus or minus ten volts.

One limitation on the use of glow discharges as relays should be noted. Oscillation, inherent in the glow itself, and resulting in "noise," makes gas tubes unsuitable to carry voice currents in communications circuits. This restriction, of course, applies to all other gas discharge devices including thyratrons. For control and signalling applications, however, this noise is unimportant.

The Cold-Cathode Tube as a Rectifier

The second circuit function which can be performed by means of cold-cathode tubes is rectification. Rectification requires an asymmetry in the current-voltage characteristic of the rectifying circuit element. When such an asymmetry exists a symmetrical voltage wave applied to the elements results in an asymmetrical current wave in the circuit. The cold-cathode-tube rectifier depends for its operation upon an asymmetrical property of the glow discharge itself. The sustaining voltage is primarily determined by the cathode fall of potential which in turn is largely dependent on the nature of the cathode material. In general low cathode falls are associated with surfaces of low work function. The slope of the current-voltage characteristic depends not only upon the nature of the cathode surface but also upon its

Table I. Characteristics and Ratings of Western Electric 313C Tube

Control-gap breakdown voltage	70 volts
Control-gap sustaining voltage	60 volts
Main-gap breakdown voltage	175 volts
Main-gap sustaining voltage	75 volts
Transfer current at anode voltage of 130 volts	5 microamperes, maximum
Deionization time	
Main gap	10 milliseconds
Control gap	3 milliseconds
Maximum instantaneous cathode current	30 milliamperes
Maximum average cathode current	10 milliamperes
Maximum time of averaging cathode current	1 second
Maximum instantaneous reverse current in main gap	5 milliamperes

area. A small cathode surface produces a steep characteristic. A glow-discharge tube then, with two electrodes one of which is large and coated with a material whose work function is low while the other

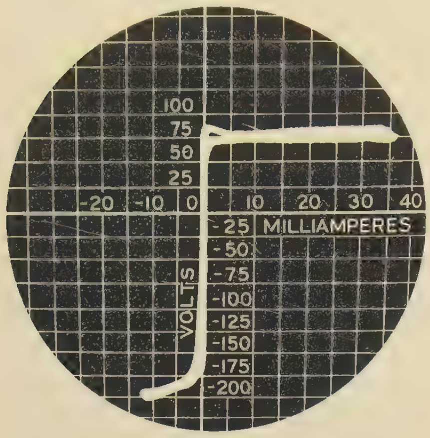


Figure 5. Asymmetrical current-voltage characteristic of cold-cathode rectifier tube

is small and uncoated, will have an asymmetrical characteristic.

The current-voltage characteristic of a Western Electric 313*C* tube connected as in the circuit of figure 6 is shown in figure 5. The trace was obtained by means of a cathode-ray oscillograph, the applied voltage being 208 volts root-mean-square, the load resistor, R_1, 5,000 ohms, and the control-gap resistor R_2, 100,000 ohms. The rectifying properties of the main gap are apparent from this characteristic. The control gap is connected into the circuit only to provide a low starting voltage in the forward direction.

Rectification can also be obtained using two-element tubes, the low starting voltage being produced by designing the tubes so that the anode-cathode spacing is small and the breakdown voltage therefore low. However, cathode sputtering from the small electrode on to the neighboring active surface causes the voltage characteristic of such tubes to be unstable so that three-element tubes are to be preferred.

As rectifiers cold-cathode tubes may be used either to convert an a-c power supply to direct current or to discriminate between positive and negative polarity in a circuit. In power supplies for radio receiving sets several tubes have attained some importance in the past,[5] although unsatisfactory tube life, radio-frequency noise arising from the discharge, and the inherent inefficiency resulting from the large voltage drop have prevented their wide adoption and in recent years their importance has been declining. As polarity detectors, however, cold-cathode tubes have a wide field of usefulness. One extensive application of this type in the communications field will be cited in the latter part of this paper.

The Cold-Cathode Tube as a Voltage Regulator

The voltage-regulating property of the glow discharge is well known. It is based upon the flatness of the current-voltage characteristic shown in figure 1. The sustaining voltage is practically independent of current. Thus in the circuits shown in figures 7*a* and *b* variations in the supply voltage will be practically entirely taken up in the series resistance, the voltage across the tube remaining constant.

Commercial voltage regulators are available which regulate at 60, 70, 90, 110, 130, and 150 volts. All such tubes may be operated in series to obtain regulation at higher voltages. The regulated voltage will in general vary less than five per cent from no load to full load and variations from tube to tube can usually be held to within ± five per cent of the nominal values. The tube illustrated in figure 2 may be used to regulate at either 60 or 75 volts, the sustaining voltages in the control and main gaps, respectively. The two circuit connections are illustrated in figures 7*a* and *b*. In the circuit of figure 7*b* the relay principle has been used to reduce the starting voltage exactly as it is when the tube is used for rectification.

Comparison of Ignitron, Thyratron, and Cold-Cathode Tube

In the introductory paragraph of this paper the functional similarity of the three types of gas-filled control tubes, thyratrons, ignitrons, and cold-cathode tubes, was stressed. These devices have this fundamental property in common. Each one contains a control element, the grid in the thyratron, the ignitor in the ignitron, and the control anode in the cold-cathode tube. Positive potential may in each case be applied to the anode without initiating gas conduction provided the control element is held more negative than some critical value. If, however, this critical value is exceeded, breakdown occurs and the gas becomes conducting. After conduction begins the control element loses all sensible control over the discharge which may be extinguished only by maintaining the anode below the sustaining voltage long enough for the tube to deionize.

Since they have these basic properties in common it is not surprising that the three types of gas-filled control tubes should be functionally similar in their circuit applications. It is scarcely too strong a statement to say that any *type* of circuit set up with one of these circuit elements can also be set up with either one of the others. In spite of this, however, they are not competitive with each other but rather complementary in their functions. This is because, while they are basically similar in principle, their operating characteristics are so widely

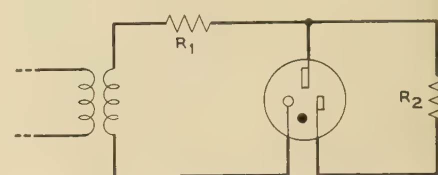


Figure 6. Cold-cathode-tube rectifier circuit

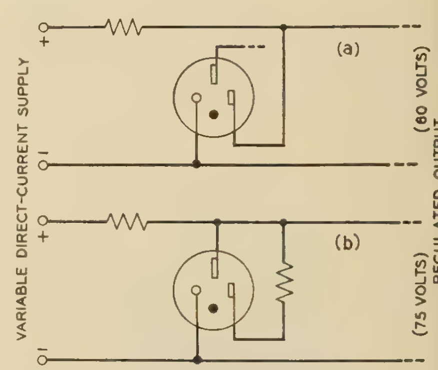


Figure 7. Cold-cathode-tube voltage-regulator circuits

different that generally in any particular case no doubt will exist as to which device is the *practical* one to use. Table II summarizes in a brief though necessarily incomplete way the comparative characteristics of the three classes of gas-filled control tubes.

The brevity of the table requires that something further be said in explanation.

In current-carrying capacity the ignitron is an inherently high-current device, the mercury-pool cathode being capable of supplying thousands of amperes of current. On the other hand, there exists a minimum current for stable operation, of approximately five amperes. Below this current the arc spot will not maintain itself. On the other end of the scale cold-cathode tubes are inherently low-current devices. At currents above about 20 milliamperes per square centimeter the active cathode surface is rapidly disintegrated by positive-ion bombardment. High current capacity then can be obtained only by the use of cathode areas

Table II. Comparison of Gas-Filled Control Tubes

Characteristics	Ignitron	Thyratron	Cold-Cathode Tube
Current capacity	5–10,000 amperes	Up to 100 amperes	Up to 100 milliamperes
Deionization time	10^{-4} second	10^{-4} second	10^{-2} second
Ionization time	10^{-5} second	10^{-6} second	10^{-4} second
Cathode heating time	0	Finite	0
Deterioration in standby service	No	Yes	No
Accuracy of characteristics	Variable	±2 volts	±10 volts
Sustaining voltage	15 volts	15 volts	75 volts

NOTE: Where specific values are given these are only approximate and are cited only to make a quick comparison possible.

of impractical size. Existing tubes are capable of supplying peak currents of approximately 100 milliamperes.

In speed of ionization and deionization the ignitron and the thyratron are much faster than the cold-cathode tube. This is because they are low-pressure devices. Argon-filled cold-cathode tubes are faster than those filled with neon-argon mixtures in which the neon predominates. The transfer currents, however, are also much higher so that in the argon-filled

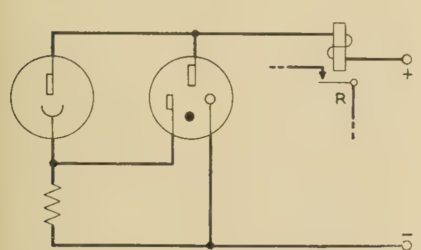


Figure 8. Photoelectric relay

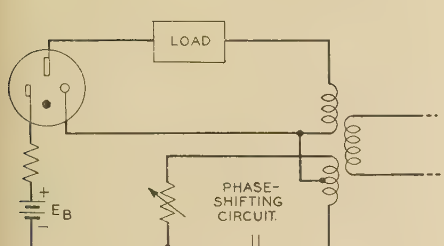


Figure 9. Cold-cathode-tube circuit with phase control

tubes sensitivity must be sacrificed in return for speed of operation.

The ignitron and the cold-cathode tube are, except for a brief ionization time, instant starting devices. The thyratron, on the other hand, requires a cathode heating time to bring the cathode to operating temperature. When the thyratron is used as a relay, the cathode must be maintained continuously at electron emitting temperature even though the periods of operation are brief and intermittent. This implies continuous deterioration of the tube even during standby periods. The necessity of supplying cathode power is itself a disadvantage. In service where the operation is intermittent much longer tube life is to be expected with cold-cathode tubes than with thyratrons.

As a voltage-operated device the thyratron is the most sensitive of the three. Its critical characteristic will generally be held within an extreme range of ± two volts. The variations in breakdown voltage of existing cold-cathode tubes will exceed this by a factor of five. Variability in ignitor rod materials causes ignitron characteristics to be most variable in this respect. For this reason an ignitron is generally controlled by an associated thyratron circuit so that the amplitude of the ignitor current may be made to exceed the critical value by a large margin while the thyratron is used as the voltage-sensitive element.

The sustaining voltage of the discharge is low, approximately equal to the ionization potential of the gas in both the ignitron and the thyratron. In the cold-cathode tube it is higher, approximately equal to the cathode fall of potential. This quantity is characteristic of the cathode material and the gas and is in general lowest for the alkalis and alkaline-earth metals associated with the rare gases. In practical applications the convenience of operation of the cold-cathode tube, since it requires neither filament transformer nor filament power, more than compensates for its lower efficiency resulting from high tube voltage drop.

Circuit Applications of Cold-Cathode Tubes

In cold-cathode-tube circuits as in thyratron circuits the problem of extinguishing the discharge, once it is initiated, must be met. The means available are four in number. The first is the use of an alternating anode voltage. After the removal of the control anode voltage conduction will then continue until the applied anode voltage falls to the sustaining voltage. Reignition will not occur on the next positive cycle if the frequency is low enough to allow the tube time to deionize. The second method is the opening of the anode circuit by a switch or relay contact. Again the length of the interruption must exceed the deionization time. The third is the application of a surge of negative voltage to the anode through a capacitor as in the familiar parallel type inverter circuit. The cold-cathode equivalent of this circuit is illustrated in figure 10 and will be described hereinafter. The fourth is the overshooting of the voltage due to the inductance of the circuit and the dynamic characteristic of the tube when a capacitor is discharged through it. This will be illustrated in considering the relaxation oscillator.

The basic relay, rectifier, and voltage-regulator circuits using cold-cathode tubes have been illustrated in figures 3, 6, and 7. Figure 8 shows a simple cold-cathode photocell relay. Increase of light intensity will cause the relay, R, to operate. Means will have to be provided for opening the anode circuit to reset the device for a second operation.

If an alternating voltage is applied to the anode of a cold-cathode tube and an alternating voltage of variable phase applied to the control anode, phase control of the output can be obtained. The circuit is shown in figure 9 and is quite analogous in its operation to phase-control circuits using thyratrons. The bias, E_B, on which the alternating voltage is superimposed should be somewhat lower than the control-gap sustaining voltage.

Figure 10 shows a cold-cathode-tube square-wave oscillator which is the ana-

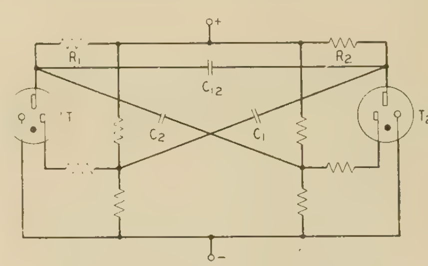


Figure 10. Square-wave oscillator

Note the similarity to the self-excited parallel-type thyratron inverter

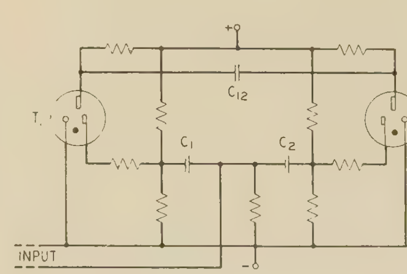


Figure 11. Counting circuit

Note the similarity to the Wynn-Williams "scale of two" circuit

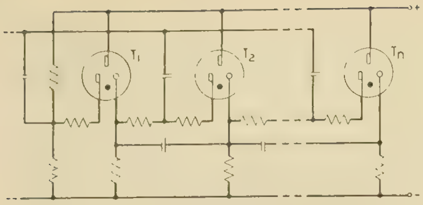


Figure 12. Chain counting circuit

logue of the self-excited parallel-type thyratron inverter. Such a circuit, of course, would not be used for the conversion of d-c power to a-c power since the cold-cathode tube is not a power device. It may be used, however, to generate a block wave to key another circuit. The operation of such a circuit is well known but will be described here since the principles are of general applicability in a variety of other circuits. Assume tube T_2 to be conducting, tube T_1 nonconducting. On the firing of T_1 its anode voltage will drop suddenly from the supply voltage to the sustaining voltage of the tube. This negative surge will be transmitted through the commutating capacitor C_{12}, extinguishing T_2 by driving its anode potential below the sustaining voltage and maintaining it there for a time determined by the time constant R_2C_{12}. A similar surge through C_2 reduces the control anode voltage of T_2 below the control gap breakdown voltage. C_2 charges up through the control gap resistors and T_2 fires when this voltage is again reached. The discharge will thus transfer back and forth between T_1 and T_2 at a rate dependent on the values of the constants in the control anode circuits.

This circuit can be modified as shown in figure 11 so that incoming positive pulses control the transfer back and forth of the discharge. Note that in this circuit the equilibrium potential of the control anodes, as determined by their associated potentiometers, should be below the control-gap breakdown voltage rather than above it as in the preceding circuit. The functioning of this circuit is analogous to that of the familiar "scale of two" counting circuit of Wynn-Williams.[6] For counting closely spaced impulses, of course, the thyratron circuit is preferable on account of the greater speed of operation of the hot-cathode tubes.

A chain of tubes, down which the discharge progresses in steps as successive impulses are fed into the input is shown in figure 12. The drop across the cathode resistor of the conducting tube "primes" the next tube in line so that it, rather than any other, is fired when the next impulse arrives. A potentiometer across tube T_1 gives its control anode an initial priming so that the first incoming pulse fires this tube preferentially. Detailed consideration of the behavior of thyratron circuits of this general class has been given recently by Shumard.[7]

Any glow discharge tube may be used in a relaxation-oscillator circuit. The simplest form of such a circuit is shown in figure 13. Oscillation will generally occur without the presence of the inductance L but its insertion causes the capacitor to discharge to a voltage well below the sustaining voltage rendering certain the extinction of the tube.

A novel use of cold-cathode tubes as relays in a device for the remote control

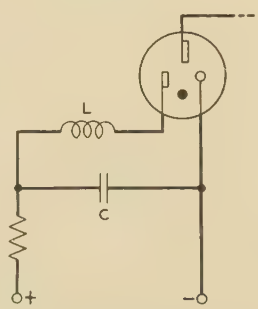


Figure 13. Simple gas-tube relaxation oscillator

of radio receivers has recently been described.[8] From a circuit standpoint the principal feature of interest in this circuit is the use of a radio-frequency signal on the control gap to fire the tube. As might be expected the peak voltage of the radio-frequency signal required to break the tube down exceeds the d-c control-gap breakdown voltage by a considerable margin.

What is probably the largest use of cold-cathode tubes to date is in an application in telephone communication where they have found extensive use in a four-party subscriber set for selective ringing.[9] The circuit is illustrated in figure 14. Of the four ringers, two are connected to one side of the line, two to the other, ground being used to complete the circuit. For selective operation of the two ringers on one side of the line a ringing signal is used which consists of an alternating voltage on which is superimposed a direct voltage. The cold-cathode tubes functioning as rectifiers are placed in series with the ringers and are oppositely poled in the two cases. One ringer responds to positive superimposed voltage, the other to negative.

One particularly valuable property of the tube in this application not possessed by rectifiers of other types is that as long as the breakdown voltage is not exceeded, the tube represents virtual open circuit to ground. Thus no transmission loss is experienced and no ground noise is introduced into the circuit when the line is used for voice transmission, the talking battery voltage having in general a nominal value of either 24 or 48 volts.

In the older type of equipment a-c relays were used to disconnect the ringers from the line except when ringing voltage was applied. Selection between the positive and negative polarities was obtained by means of mechanically biased ringers.

Summary

Three types of gas-filled control tubes are now in common use. The properties of cold-cathode tubes, the most recent of these to receive extensive application, have been considered and comparisons drawn with those of the more familiar thyratron and ignitron. It is concluded that in its own field of low-current control devices the cold-cathode tube has several inherent advantages which will ensure a wide use for it in the future. These advantages are, the ability to operate without cathode heating power, the ability to start immediately when a signal is applied, and the absence of deterioration in standby service.

A number of typical circuits illustrating the capabilities of the tubes as circuit

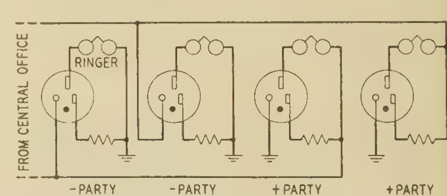


Figure 14. Four-party selective ringing circuit

elements have been described. Several of these are in commercial applications. One, involving some hundreds of thousands of tubes, has been operating for several years and proves beyond doubt that the cold-cathode tube is a valuable addition to the array of control devices available to the circuit engineer.

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Polyphase Broadcasting

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THE universal use of amplitude modulation in radio broadcasting service has led to several formidable problems in the design of broadcast transmitting equipment. Improvements in technique which permitted the complete modulation of a carrier (100 per cent modulation) raised new problems in connection with the conversion efficiency of the final radio-frequency amplifier stage. Continued research has resulted in such developments as the high-level plate-modulated amplifier, the high-efficiency linear amplifier, outphasing modulation, and others.

In all of these high-efficiency systems, the radio-frequency output of the transmitter is amplitude modulated, and at 100 per cent modulation, the radio-frequency power output of the transmitter varies all the way from zero to four times the carrier power during each cycle of the audio-frequency modulating voltage. It is thus apparent that the final power-amplifier tubes must be capable of delivering a peak power equal to four times the carrier power of the transmitter.

The unique feature of the system to be described is the fact that the radio-frequency power output of the transmitter is constant throughout the audio-frequency modulation cycle, and is equal to $1^1/_2$ times the carrier power. This feature indicates the possibility of reducing the high-power tube complement to a very minimum, and it is conceivable that the peak power capacity of the final amplifier tubes might be as low as $1^1/_2$ times the carrier power instead of the twice carrier value of the present paper.

The comparison between the polyphase broadcasting system and the system of present-day broadcasting is somewhat similar to the comparison between a polyphase generator and a single-phase generator in that the instantaneous power output of a polyphase generator supplying a balanced load is constant, while in the case of the single-phase generator, the power varies sinusoidally from zero to twice the average power during the cycle.

It will be shown that the "rotating modulation field" essential to polyphase broadcasting may be obtained by the use of a five-element vertical antenna array. Four identical radiators are arranged at the corners of a square with the fifth antenna in the center. The central antenna is supplied with unmodulated radio-frequency power and is called the carrier antenna. The antennas at opposite ends of one diagonal constitute a directional pair and are supplied with suppressed-carrier modulated radio-frequency power. The two antennas at the ends of the other diagonal also constitute a directional pair and are fed with suppressed-carrier modulated radio-frequency power. The two antennas of each side-band pair are fed in series and carry currents of opposite phase, so that the directional patterns are figures of eight at right angles. Further, the phase of the suppressed-carrier currents in the side-band pairs differs from the phase of the current in the carrier antenna by an angle of 90 degrees. If in addition, the modulating voltage in the suppressed carrier channels is in quadrature, the required conditions, for a rotating modulation field, are fulfilled.

Paper number 39-46, recommended by the AIEE committee on communication, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 5, 1938; made available for preprinting January 3, 1939.

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The Radiating System

The fundamental arrangement of the antenna system is shown in figure 1. The antenna at the point O is the carrier antenna. The antennas 1 and 1′ comprise a directional pair, and carry equal and opposite double side-band currents. Similarly, antennas 2 and 2′ comprise a directional pair and are placed physically at right angles to the first pair.

Let the following currents be established in the antenna system:

$$
\begin{aligned}
i_O &= I_O \cos \omega t && \text{(carrier)}\\
i_1 &= I_1 \sin \omega t \sin \rho t && \text{(double side band)}\\
i_1' &= -I_1 \sin \omega t \sin \rho t && \text{(double side band)}\\
i_2 &= I_1 \sin \omega t \cos \rho t && \text{(double side band)}\\
i_2' &= -I_1 \sin \omega t \cos \rho t && \text{(double side band)}
\end{aligned}
$$

where

i_O = instantaneous current in antenna O
i_1 = instantaneous current in antenna 1
i_1' = instantaneous current in antenna 1′
i_2 = instantaneous current in antenna 2
i_2' = instantaneous current in antenna 2′
ω = 2π times carrier frequency
ρ = 2π times modulating frequency
I_O = maximum value of current in carrier antenna
I_1 = maximum value of current in side band antennas

The field intensity E, at a remote point P, with arbitrary reference angle θ and at a distance r_O meters from the central antenna, becomes:

$$
E = \frac{KI_O}{r_O}\cos\omega\left(t-\frac{r_O}{C}\right) + \frac{KI_1}{r_O}\left\{\sin\omega\left(t-\frac{r_O-d\cos\theta}{C}\right) - \sin\omega\left(t-\frac{r_O+d\cos\theta}{C}\right)\right\}\sin\rho t + \frac{KI_1}{r_O}\left\{\sin\omega\left(t-\frac{r_O+d\sin\theta}{C}\right) - \sin\omega\left(t-\frac{r_O-d\sin\theta}{C}\right)\right\}\cos\rho t
$$

Where K is an antenna performance constant, all antennas assumed identical, C is the velocity of light in meters per second.

The above expression after some reduction, becomes:

$$
E = \frac{KI_O}{r_O}\cos\omega\left(t-\frac{r_O}{C}\right)\left[1+\frac{2I_1}{I_O}\times\left\{\sin\left(\frac{\omega d\cos\theta}{C}\right)\sin\rho t - \sin\left(\frac{\omega d\sin\theta}{C}\right)\cos\rho t\right\}\right]
$$

Now, if the spacing of the antennas be restricted so that sin $(\omega d/c)$ differs only slightly from $\omega d/c$, we have approximately

$$
E = \frac{KI_O}{r_O}\cos\omega\left(t-\frac{r_O}{C}\right)\times\left[1+\frac{2\omega d I_1}{CI_O}\sin(\rho t-\theta)\right] \quad (1)
$$

Inspection of equation 1 shows that the

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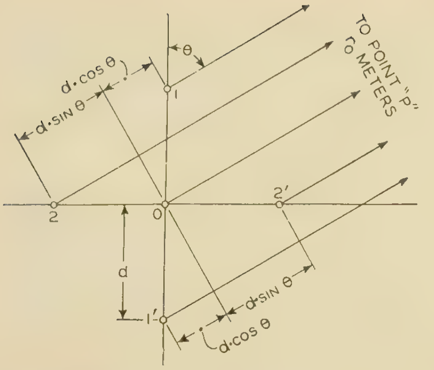


Figure 1. Antenna plan

field intensity at the point P is amplitude modulated, and that the phase of the modulation varies as the azimuth angle θ.

The modulation may be said to be 100 per cent when $(2\,\omega d I_1)/(c\,I_O) = 1$.

Further, the value of I_1, the maximum value of current in the side-band antennas is, for 100 per cent modulation

$$I_1 = \frac{C}{2\omega d} I_O$$

$\frac{\omega d}{C}$ is the antenna spacing in radians S

Hence

$$I_1 = \frac{I_O}{2S}$$

Thus, for a spacing of one-half radian, the maximum side-band antenna current is equal to the maximum carrier antenna current in the 100 per cent modulated condition.

Referring again to figure 1, the following items should be noted:

1. Radiators 1 and 1′, since they carry equal and opposite currents, induce no voltage in antennas O, 2, or 2′.

2. Radiators 2 and 2′, since they also carry equal and opposite currents, induce no voltage in antennas O, 1, or 1′.

3. Radiator O induces equal voltages in antennas 1 and 1′, and 2 and 2′. Inasmuch as these antenna pairs are fed in series, the induced voltages are in opposition, and thus produce no current.

The full significance of the above points is grasped when it is realized that the carrier antenna, side-band pair 1, 1′, and side-band pair, 2, 2′, are completely decoupled, and each may be fed by its own generator (amplifier) without affecting the others.

In the derivation of the expression for the field intensity at the point P, the formula was simplified by making the assumption that the electrical spacing was small, less than 0.5 radian. This consideration is fortunate, for in practice it means that the side-band radiators can conceivably be suspended from the main carrier radiator. For example, with a spacing of one-third radian, the following situation exists at a frequency of 1,000 kilocycles:

One wave length = 985 feet
One-third radian = 52.2 feet

The required spacing is thus 52.2 feet, which is an entirely feasible value, permitting the central radiator to support the side-band radiators.

Another desirable result of the close spacing is that the high-angle radiation distribution of the side-band system closely approaches that of the central antenna, so that the per-cent modulation is substantially equal for all radiation angles.

The spacing, however, must not become too small, for some difficulty would undoubtedly be experienced, first in the balance of the mutual effects of the independent systems, and second, the radiation resistance of the side-band pairs at the current loop diminishes approximately as the square of the spacing. For a spacing of one-third radian, the resistance at the current loop for the side-band pairs may be calculated to be 22.2 per cent of that of the central antenna. It is a rather simple matter properly to excite this side-band system.

The Transmitter

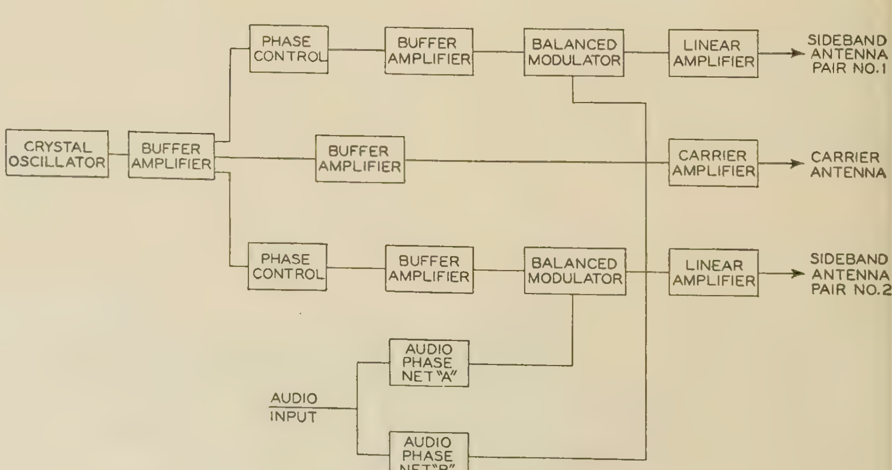
A block diagram of a typical polyphase transmitter is shown in figure 2. It is essentially a three-channel transmitter frequency-controlled from a common crystal oscillator and appropriate buffer amplifiers. The carrier channel is a straightforward continuous-wave telegraph design and sufficient amplifiers cascaded to obtain the desired carrier power level. The output of the carrier amplifier is coupled by conventional means to the carrier antenna. Each of the side-band channels consists of a final class-B radio-frequency amplifier excited by a balanced modulator. The balanced modulator, in turn, receives its excitation from the crystal oscillator through a phase-shifting goniometer, and the necessary buffer amplifiers. The output of each side-band channel is fed to the appropriate pair of side-band radiators. The phase-shifting goniometer is employed in each of the side-band channels to provide easy adjustment of phase so that the phase of the double side-band current in the side-band antennas can be set at its proper value with respect to the radio-frequency current in the carrier antenna.

Polyphase Audio System

All of the foregoing material would be an interesting academic consideration only, if it were impossible to design a satisfactory source of two-phase audio-frequency modulating voltage. Two possible systems were studied.

The first system given consideration was the following. A single-side-band signal is obtained using a 50-kilocycle suppressed-carrier modulator and appropriate filters. The resulting single-side-band signal is impressed on the grids of two demodulators, one demodulator being supplied with the original carrier, and the other with the original carrier shifted 90 degrees in phase. The outputs of the demodulators will be in phase quadrature. This system, while producing perfect quadrature voltages, is seriously limited in its low-frequency response due to the nature of the filters required. The limitation is so severe, that the best single-side-band systems today are acceptable only between frequencies of 100 and 6,000 cycles. This frequency

Figure 2. Block diagram of polyphase transmitter

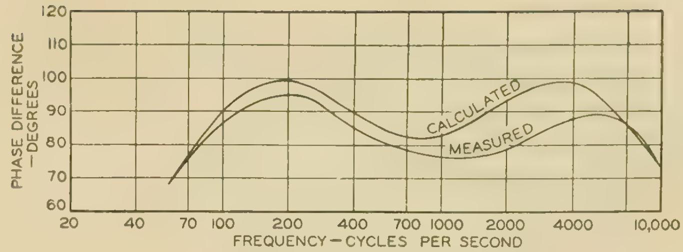


band is not sufficient for high-fidelity broadcasting, and consequently, this source of two-phase audio was discarded.

The second system, and the one adopted, makes use of non-dissipative phase-delay networks of the lattice type. The audio-frequency voltage is impressed across two networks, each having three sections, and the circuit constants in each section are so chosen that the *difference* in the phase delay of the two networks is substantially 90 degrees throughout the frequency range 30–10,000 cycles.

Figure 3 is the calculated and measured performance of a pair of two-section networks. In this graph, the deviation from the desired value of 90 degrees is shown as a function of frequency. Figure 4 is the calculated performance of a pair of three-section networks.

Figure 3. Performance of a pair of two-section phase-shifting networks



Practical Aspects of the System

Vacuum-Tube Complement

It is clear that, for a 50-kw transmitter, the carrier amplifier must be capable of supplying a peak power of 50 kw. Under no condition is its load any greater than 50 kw. Further, at 100 per cent modulation, the power output of the system is constant at $1^1/_2$ times carrier power.

The combined output of the side-band amplifiers is hence equal to one-half the carrier at every instant of the 100 per cent modulation cycle. Each of the side-band pairs is idle twice every cycle and during this time the operating side-band pair must supply all the side-band power. It is therefore apparent that the side-band amplifiers *each* have a peak capability of one-half carrier power. In the case of a 50-kw transmitter, therefore, each side-band linear amplifier must have a peak capacity of 25 kw.

The total tube capacity in the final stage of the transmitter is hence equal to two times the carrier power, in contrast with four times carrier power in present day equipment.

The reduction in the number of vacuum tubes required in the final stage of a high-power transmitter is at once apparent. Comparing a possible arrangement for a 500-kw polyphase transmitter with the arrangement at WLW, it can be shown that at least 11 of the 20 100-kw vacuum tubes now used would be eliminated. The saving in filament power consumption alone, would be of the order of 80 kw.

Efficiency of Final Amplifiers

The efficiency of the carrier amplifier will be nominally 70 per cent so that in the case of a 50-kw transmitter, the plate input to this stage will be 71.4 kw. In the case of the side-band amplifiers, assume that the peak efficiency during a modulation cycle is 70 per cent. This efficiency will be obtained at the maximum output of 25 kw. Assuming that the plate current of the linear amplifier is proportional to the radio-frequency grid voltage, the instantaneous power input p will be

$$p = E_B I_B |\sin \rho t|$$

where E_B = direct plate voltage in kilovolts, I_B = maximum value of plate current in amperes. The d-c input power P is the average of the above expression.

$$P = \frac{2E_B I_B}{\pi}$$

Also it was assumed that, for 25-kw peak output, the efficiency was 70 per cent. Therefore

$$E_B I_B \times 0.7 = 25$$

The average power input to each side-band amplifier is thus

$$P = \frac{2 \times 25}{\pi \times 0.7} = 22.7 \text{ kw}$$

The average output of each side-band amplifier is 12.5 kw and the efficiency is 12.5/22.7 = 55 per cent.

At 100 per cent modulation, the total power input to the last stages is

Carrier amplifier	71.4 kw
Side-band amplifier number 1	22.7 kw
Side-band amplifier number 2	22.7 kw
Total	116.8 kw

The output, 100 per cent modulated, is 75 kw and the efficiency of conversion is 75/116.8 = 64 per cent. This efficiency figure is comparable to other high-efficiency systems.

Exciter Circuit Requirements

In the case of the high-efficiency linear amplifier, the exciter circuit must be of sufficient capacity to drive the final amplifier to a peak output of four times carrier power. For example, in the case of a 50-kw transmitter, the exciter must be capable of substantially linear performance for a final amplifier output range, 0 to 200 kw. In the polyphase system, however, each side-band amplifier exciter is required to drive its amplifier linearly only in the range 0 to 25 kw. This fact results in a reduction in exciter capacity, and in fact, radiation-cooled tubes may readily be used for excitation of the final amplifiers for 50-kw transmitters. The exciter for the carrier amplifier may likewise consist of radiation-cooled tubes. Inasmuch as the total capability of the final amplifiers is only one-half as great as in conventional systems, the exciter capability is likewise half as large as is ordinarily required. As a matter of fact, the exciter capacity is somewhat less than one-half that required for a linear amplifier, for it is unnecessary to provide a stabilizing load for the carrier amplifier exciter.

Fidelity of Transmission

The effect of the phase of the audio signals being different from 90 degrees results in nonuniform percentage of modulation in different directions. Equation 1 was derived on the assumption that the audio-frequency modulating voltage was in phase quadrature. If the audio-frequency voltages are not in phase quadrature, the expression for the field is modified according to the following expression:

$$E = \frac{KI_O}{r_O} \cos \omega \left(t - \frac{r_O}{C} \right) \left[1 + \frac{2SI_1}{I_O} \times \left\{ \sin \rho t \cos \theta - \cos (\rho t - \phi) \sin \theta \right\} \right]$$

where ϕ is the deviation from the desired 90 degrees.

Inspection of this relation shows that the greatest effect due to phase deviation will occur for θ = 45 degrees, 135 degrees, 225 degrees, and 315 degrees.

The following table shows the effect of small deviations upon the per-cent modulation at an angle θ = 45 degrees.

Phase Deviation	Decibel Change in Amplitude
+10	−0.82
+ 5	−0.38
0	0
− 5	+0.36
−10	+0.68

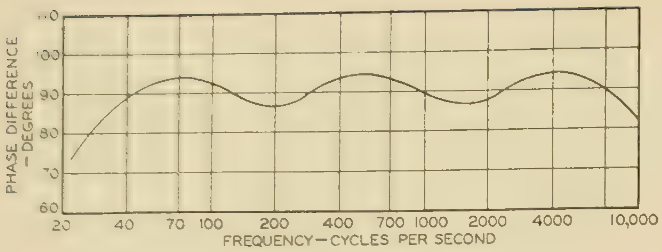


Figure 4. Performance of a pair of three-section phase-shifting networks

The change in modulation level for small deviations from the desired value of 90 degrees is thus seen to be small.

If the phase relation between the audio signals is different for signal components of different frequencies, the result in any particular direction is the same as that produced by varying frequency response in the conventional transmitter. As noted above, if the phase does not vary from 90 degrees by more than ±10 degrees the effect is a maximum variation in frequency response of less than one decibel. Thus a deviation from the desired 90-degree phase relation of the modulating voltages produces frequency distortion.

The effect of an improper phase adjustment of the side-band amplifiers with respect to the carrier is to produce amplitude distortion. To make this clear refer to figure 5. In this figure the line OC is the carrier vector. The side-band vectors are oppositely rotating and their sum lies along the line $S-$, $S+$. At any particular instant in the modulation cycle the sum of the carrier and the two side-band vectors might be represented by the vector OP. As the modulation progresses through the cycle, the point P moves back and forth along the line $S-$, $S-$ in simple harmonic motion. A linear detector would merely provide a current proportional to the magnitude of the vector OP as a function of time. Consequently if the side-band phase is incorrect by as much as 30 degrees serious amplitude distortion would occur at high modulation percentages, for the length of the vector OP obviously does not vary sinusoidally. On the other hand, if the side-band phase is no more than three degrees from the desired value, a glance at figure 5 is sufficient to indicate that the amount of distortion occurring would be rather small. In practice, a remote monitoring point would be selected, and the modulated radio-frequency pattern would be transmitted back to the control point at a low intermediate frequency—say 50 kilocycles. This pattern would then be viewed on a cathode-ray oscilloscope and proper adjustment of the phase of the side-band amplifiers with respect to the carrier would permit full 100 per cent modulation to be obtained.

Experimental Data

A systematic program of experimental work is in progress to substantiate the fundamental operations of the system

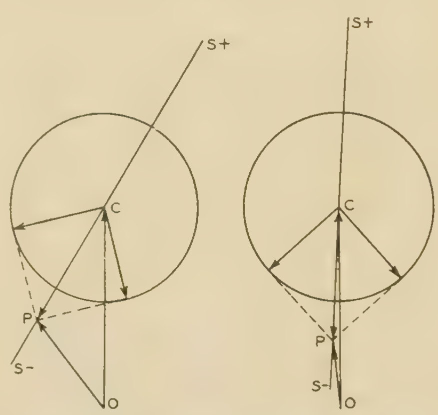


Figure 5. The effect of side-band phase adjustment

and to make a detailed study of broadcast-signal fidelity.

The first preliminary work was carried out with a transmitter having about 100 watts carrier power and operating on about 80 meters. Preliminary measurements showed complete accord with the above theory of operation. A check was made using two-phase 60-cycle audio frequency for modulation of the transmitter and 60 cycles from the same source was used to modulate a second conventional transmitter on a different frequency. By the use of two similar receivers the demodulated 60-cycle output from the two receivers was compared, and the phase between these two outputs varied from 0 to 360 degrees as the receiving point was moved around the antenna system.

The audio-frequency phase-difference network of figure 2 was then inserted and the polyphase transmitter operated on voice modulation. The fidelity of operation was equivalent to that of conventional transmitters. No particular attempt was made in these first experiments to measure audio-frequency harmonic distortion or to make a detailed study of fidelity. No effort was made to provide an accurately spaced antenna system or to provide low-distortion suppressed-carrier modulators in this first model.

In order to make a detailed experimental study of the broadcast signal fidelity, a second project is under way. This project consists in construction of a 1,000-watt broadcast-frequency transmitter. Experimental data will be obtained using an array of half-wave vertical radiators.

Conclusions

A new system of broadcasting amplitude-modulated radio-frequency signals has been described. It is felt that this system has great possibilities and the essential features are high efficiency of power conversion and minimum tube capacity requirements for the final amplifiers. The inherent disadvantage of such a system lies in the fact that an essential requirement is a nondirectional radiation pattern. It is felt that the complication of circuits and adjustments over that of conventional transmitters will further limit the application of this system to high powered equipment (50 kw and larger) where power consumption and tube replacement are formidable items of expense.

The technical possibility of superpower transmitting equipment of the order of 1,000 to 2,000 kw, is brought into the realm of reality using tubes now available.

A 12-Channel Carrier Telephone System for Open-Wire Lines

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Synopsis: A new carrier telephone system is described, together with its application in the long-distance telephone plant. By its use, an open-wire pair which already furnishes one voice circuit and three carrier circuits may have 12 more telephone circuits added. Thus in all 16 telephone circuits are obtained on a single pair. Several such systems may be operated on a pole line.

Various problems incident to the extension of the frequency range, from about 30 kilocycles, the highest frequency previously used, to above 140 kilocycles, are discussed. Among the more important of these are the control of crosstalk between several systems on a pole line, arrangements for taking care of intermediate and terminal cables, and automatic means for compensating for the effects of weather variations on the transmission over this wide frequency range.

BARE WIRES supported on insulators, stretched between poles, make up the pioneer electrical communication circuit, the open-wire line. Although great advances have been made in the application of cable structures, the open-wire lines still hold their own in some sections of the country. This is because, to offset their physical vulnerability, they have several unique virtues. They are flexible and permit adding one pair of wires at a time. They are also comparatively economical where conditions favor their use. Furthermore they are low-attenuation circuits and for this reason were the first to be used for high-frequency carrier systems.

The first carrier systems, beginning in 1918, added three or four channels to the existing voice circuit on a pair. To keep pace with this development, improvements in transposition systems were devised so that many such carrier systems might be operated on the same pole line. Such carrier systems, typified by the three-channel type C[1,2] system, have seen continuous growth in use in the long-distance plant. Now a 12-channel system, the type J, is being made available to add to the type C system, thus giving 16 telephone circuits on an open-wire pair in addition to the two telegraph circuits. Since there are already about 60,000 miles of pole line equipped with type C systems, the new type J system was developed to go in the frequency range above the type C system rather than to supersede it with more channels (figure 1).

The new system has been designed to meet high standards of transmission and reliability for distances up to several thousand miles. The frequency band transmitted by the individual derived circuits is exceptionally wide, from about 100 to 3,600 cycles for a single system and has been previously discussed[3] in relation to the channel spacing in this and other new broad-band developments.

An important feature of the work on the type J system has naturally been that of making the line circuits suitable for carrying the higher frequencies. The tendency of circuits to crosstalk into one another increases rapidly with frequency. Advances in transposition design and structural improvements have now made it possible to extend the frequency range from 30,000 cycles to 140,000 cycles, which is about the upper frequency of the type J system. The problem of incidental cables in open-wire lines has also been serious, since the losses increase with frequency, and what is usually more important, there may be substantial reflection effects at junctions of the open-wire line and cable. These are serious, not only from the standpoint of the transmission loss which they entail, but from their effect on crosstalk. The increase in attenuation at the higher frequencies has also brought other problems into the picture. For example, repeaters are needed at more frequent intervals than with the lower-frequency systems. Attenuation variation with frequency due to weather changes is greater than at the lower frequencies.

Figure 2 shows schematically the complete type J system, with its different major circuit elements, resulting at the terminals in the division of the single line circuit effectively into 16 talking circuits. In no recent development is the function of the wave filter in providing essential units in a frequency dividing plan more forcefully illustrated than in the application of this new system, in combination with the type C and other facilities which exist. There are about 60 different designs of filters and networks in the terminals and repeaters. Their functions are varied, as, for example, separating the individual channel bands, separating the opposite directional groups of channels, separating the type J frequency range as shown in figure 1 from the type C and other ranges, separating the different carrier frequencies of a carrier supply system in which the carriers are all derived from a common 4,000-cycle source, etc.

The new system, as in the case of the type C, uses single-sideband transmission with carrier elimination. Copper-oxide units are employed as translator elements of various kinds—modulators, demodulators, and harmonic producers. Methods of mounting the equipment, and methods and apparatus for testing follow lines already worked out for the type K cable carrier system, which was described a year ago in two AIEE papers.[4,5]

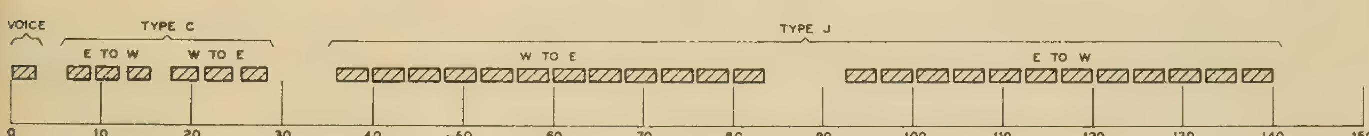

Figure 1. Frequency allocations

Channel Terminals

A terminal of the type J system changes 12 independent voice channels into a compact block of 12 carrier channels

Paper number 39-53, recommended by the AIEE committee on communication, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 25, 1938; made available for preprinting December 28, 1938.

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1. For all numbered references, see list at end of paper.

properly allocated in frequency for transmission over the open-wire line. Inversely, such a block received from the open-wire line is separated and transformed into 12 independent voice channels. The first step in transmitting the 12 voice channels is to modulate them on 12 carrier frequencies 4 kilocycles apart from 64 to 108 kilocycles and to select the lower sidebands by means of quartz-crystal channel band filters. The last step in the conversion from a received 12-channel block to the 12 independent voice channels consists in the division of the block by 12 quartz-crystal channel filters and the demodulation of these messages to produce voice frequency transmissions. These two frequency changes and separations are performed by the same equipment that is used in the type *K* cable carrier system terminals.

Figure 3 shows the circuit of a modulator and a demodulator for the opposite directions of a single conversation with indicated connections for the 11 others which make up this fundamental 12-channel block. The modulator consists of a bridge assembly of copper-oxide varistors and is supplied with about 0.5 milliwatt of carrier power from the carrier supply system which is described later. Of the two resulting sidebands, the lower is selected by the crystal band filter following the modulator. The line sides of 12 modulator band filters are joined in parallel and a compensating network is connected to preserve the band characteristics of the upper and lower channels.

On the receiving side, after separation by one of the 12 parallel filters, the side band is applied to a demodulator supplied with the proper carrier frequency to restore the voice-frequency message. Because of the low level at which demodulation takes place, the demodulator is followed by a single-stage amplifier to produce the level desired in the voice-frequency circuit. The gain of this amplifier is adjustable over a moderate range.

The combination of a single modulator and a single demodulator and associated equipment shown in figure 3 is called a "modem" and two of these are mounted on a single equipment panel. Nine of these panels, sufficient for $1^1/_2$ type *J* systems, or 18 conversations, mount in a single relay rack bay of standard height.

Carrier Supply

The carrier frequencies 64–108 kilocycles are all derived as harmonics of a 4-kilocycle frequency produced by a tuning-fork-controlled oscillator. This frequency is applied to an easily saturated coil to produce a sharply peaked wave which is rich in odd harmonics. Even harmonics of four kilocycles are obtained by rectification in a copper-oxide unit of part of the odd harmonic output. Odd and even harmonics appear in separate circuits from which each frequency desired is separated by a quartz-crystal filter. Frequencies as high as the 121st harmonic, that is, 484 kilocycles, are obtained in this way from the carrier supply system. Because of the importance of the carrier supply two sources are provided, with automatic equipment to transfer rapidly from the regular to the emergency source.

Group Modulation

As shown in figure 1, the type *J* system uses a band of 36 to 84 kilocycles for the west to east direction of transmission and 92 to 140 kilocycles for the east to west direction. The output of the fundamen-

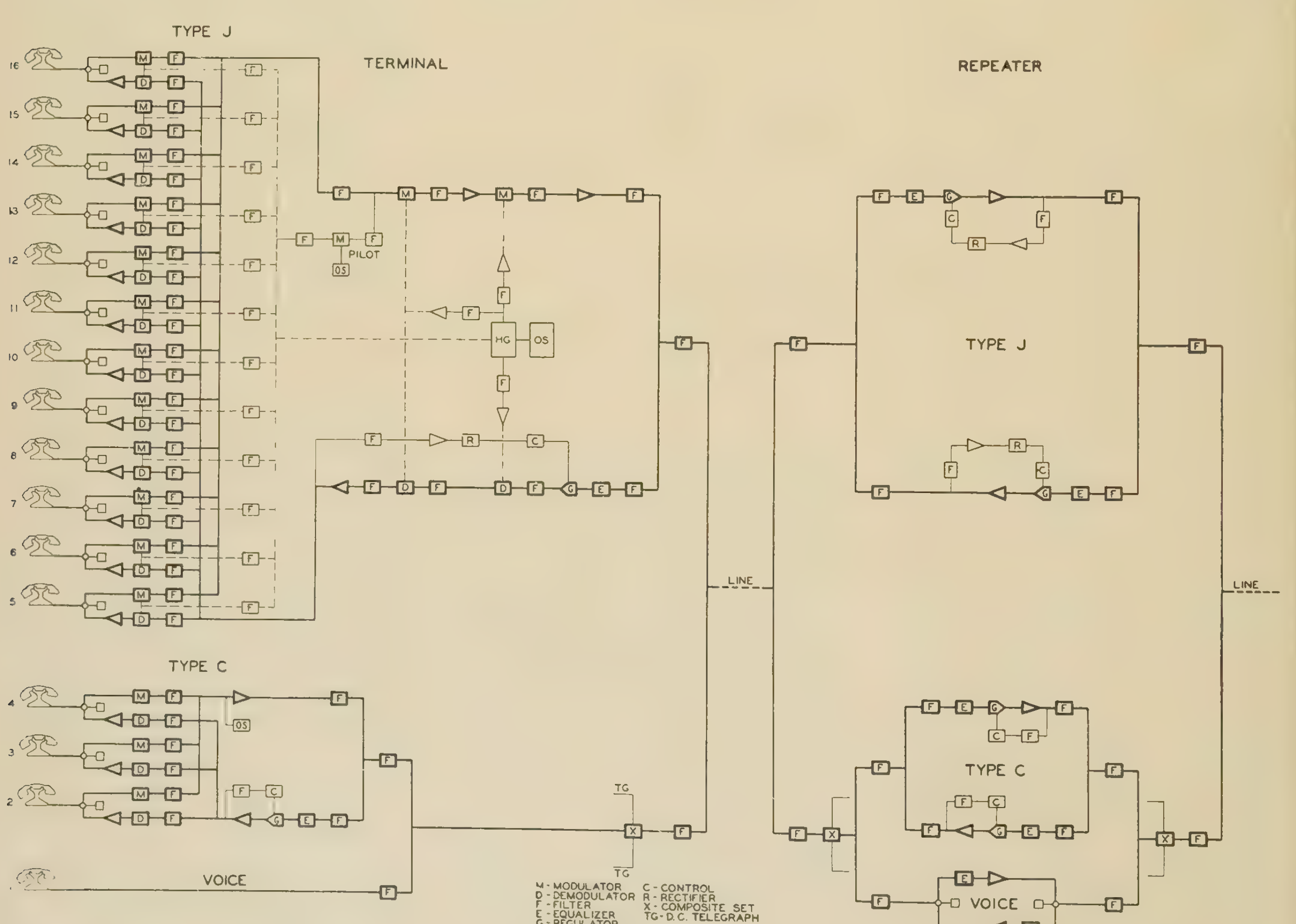


Figure 2. Terminal and repeater layout

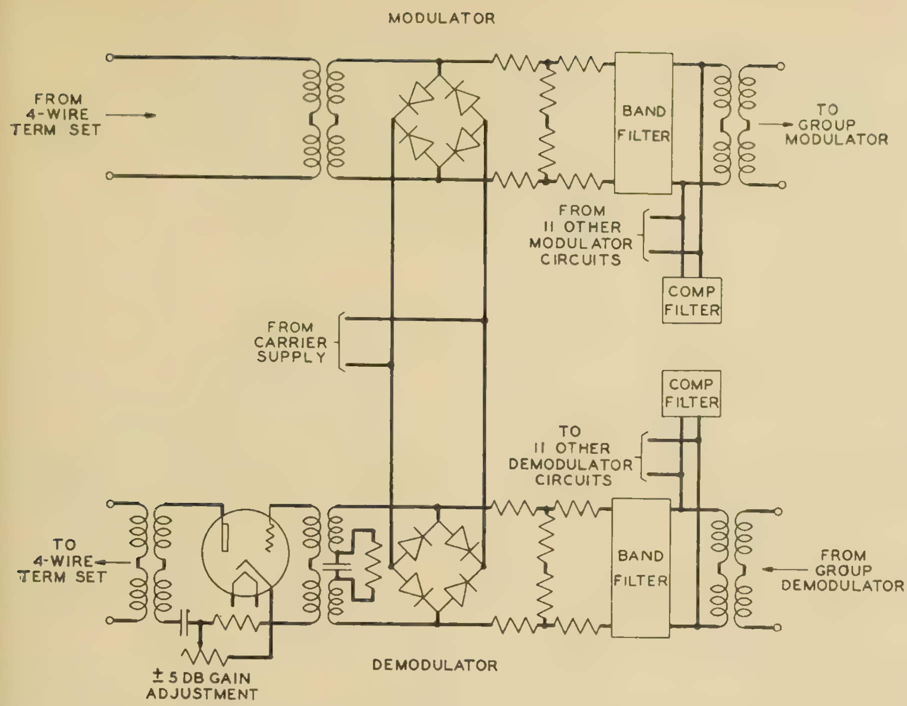


Figure 3. Channel modulator and demodulator

tal 12-channel unit consists of 12 lower sidebands from carriers of 64–108 kilocycles. This must, therefore, be translated to the two type *J* directional groups for line transmission. Since the frequencies in the fundamental unit overlap those in both directions of line transmission, this transfer must be made in two steps. Figure 4 shows these frequency translations. The first group modulation is the same for both directions of transmission. By modulating the fundamental unit with a carrier of 340 kilocycles there is obtained a block of lower sidebands extending from 400 to 448 kilocycles. A second modulation with a 484-kilocycle carrier then gives, for transmission from west to east, a 12-channel block of upper sidebands extending from 36 to 84 kilocycles. For the east to west transmission the second modulation uses a 308-kilocycle carrier, producing a 12-channel block of lower sidebands between 92 and 140 kilocycles.

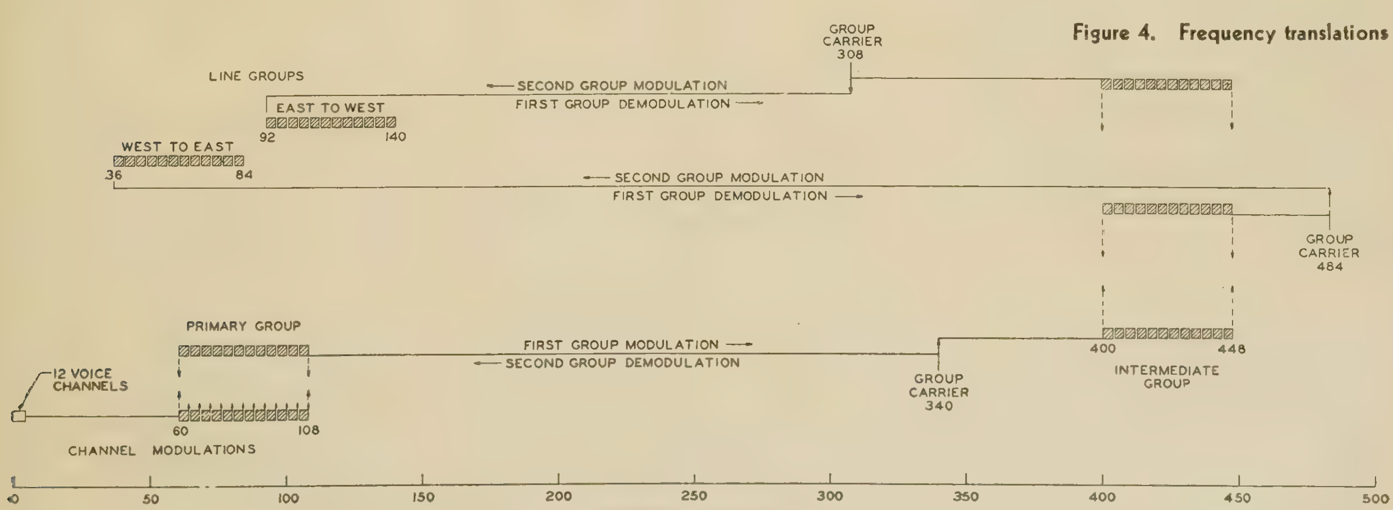
Frequencies as high as 308, 340, and 484 kilocycles are chosen for group modulation in order that undesired products shall be well separated from desired products to permit their elimination by simple filter structures.

The same group modulation processes that have been described above for adapting the 12-channel group for line transmission are used in the opposite sequence for receiving the block from the line and preparing it for separation by the channel band filters of the receiving terminal; thus, for instance, at an east terminal the block of upper sidebands, extending from 36 to 84 kilocycles as received from the line, is first modulated with 484 kilocycles producing lower sidebands between 400 and 448 kilocycles. These are next modulated with 340 kilocycles which produces a block of 12 lower sidebands extending from 60 to 108 kilocycles, which is the group that the fundamental 12-channel terminal unit is designed to handle.

Figure 5 shows the essential features of the group modulating and group demodulating circuits. As in the type *K* system, group modulation is performed at a very low level of the message material and with a high level, about 25 milliwatts, of the group carrier supply, in order to minimize interchannel crosstalk. The group modulators are of the doubly balanced bridge type which aids in suppressing some of the unwanted modulation products. Following the first group modulator and also following the first group demodulator are coil and capacitor type 400–448 kilocycle band filters which reject the unwanted products and pass the band of frequencies containing the 12 channels. Between this filter and the second group modulator on the transmitting side of the terminal, an intermediate amplifier is used in order to keep the level of the group transmission above danger of noise. Following the second group modulator and also following the second group demodulator are low-pass filters which cut off frequencies above about 160 kilocycles, to suppress unwanted modulation products. From the output of the receiving low-pass filter the 12-channel group, 60–108 kilocycles, passes through a two-stage "auxiliary" amplifier to bring it to the desired level.

The carrier frequencies for group modulation and for group demodulation are derived from the same four-kilocycle tuning-fork-controlled oscillator that supplies carriers for the 12-channel unit. From the circuit in which appear the odd harmonics of 4 kilocycles, the 77th, 85th, and 121st harmonics, that is, 308, 340, and 484 kilocycles, are selected by carrier

Figure 4. Frequency translations



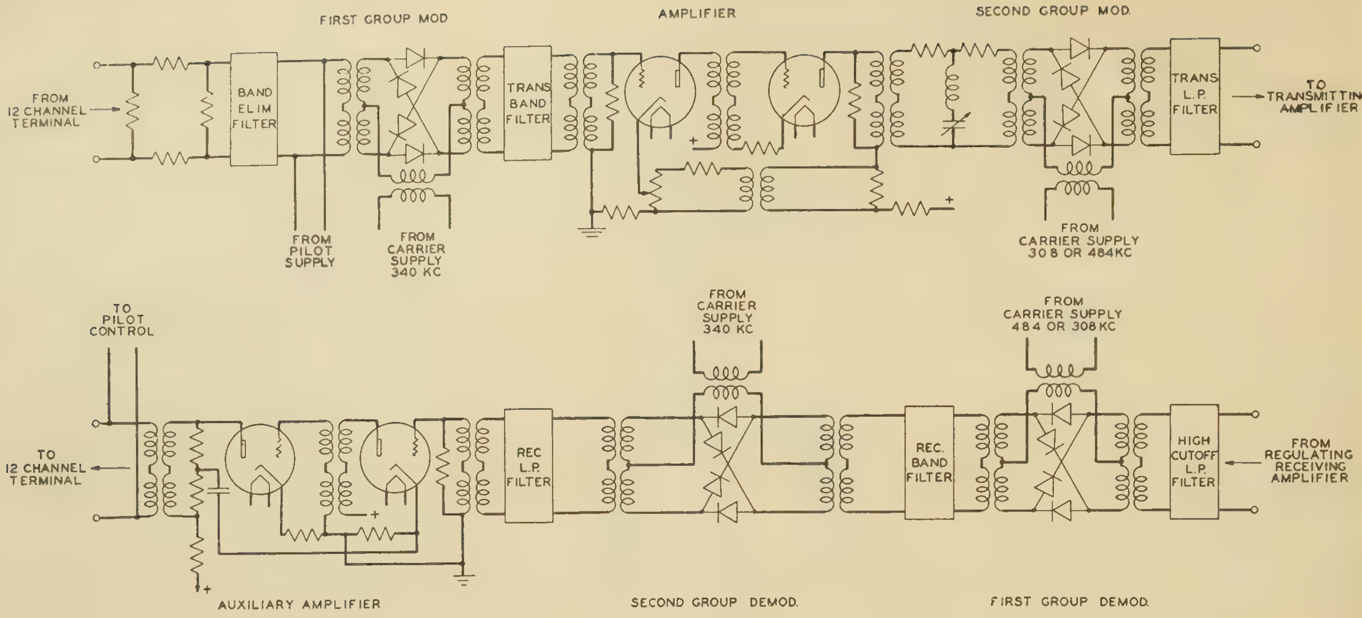


Figure 5. Group modulator and demodulator

supply filters and separately amplified by two-stage amplifiers to produce the powers required for group modulation. Outputs from these amplifiers are fed to individual-frequency busses capable of supplying the group modulators and demodulators for ten systems. An emergency carrier supply for these frequencies is also provided with arrangements for switching rapidly from the regular to the emergency circuits.

Terminal Amplifiers

As indicated on figure 5, the transmitted 12-channel group, now transferred to the proper frequency range for line transmission, goes from the low-pass filter at the output of the second group modulator to a transmitting terminal amplifier which is similar in most essentials to the amplifiers of the line repeaters. The 12-channel group arriving from the line passes through a regulating amplifier arranged and controlled to compensate for variations in equivalent of the adjacent line section before passing to the first group demodulator. Similar regulating amplifiers are used at all repeater points.

Filters

At terminals and also at repeater points, two kinds of filter sets are required. One kind is used in the line to separate the type *J* frequency range 36 to 140 kilocycles from the type *C* and other lower frequencies on the line. The second kind is the directional filters of the type *J* system itself. These separate a 12-channel band of frequencies lying below 84 kilocycles used for west to east transmission from the 12-channel group lying above 92 kilocycles which is transmitted from east to west. These directional filter sets are carefully designed to equalize any nonuniformity of loss in both the directional and the line filters. As this equalization involves a considerable loss over a large part of the filter band it is provided entirely in the receiving directional filters where the transmission is at a low level and the loss can readily be made up by amplification. In this way nearly the full energy output of the transmitting or repeater amplifier is available for line transmission.

Line Crosstalk Problems

As noted previously, type *J* systems will, in general, be applied on pairs on which type *C* systems are already operating. Such pairs have already been arranged to transmit frequencies up to 30,000 cycles, and transposed in such a manner as to perform satisfactorily as regards crosstalk to and from nearby pairs on which similar carrier systems are operating. In addition, on most modern lines the spacing between wires of a pair has been reduced from 12 to 8 inches; and, on many of the lines, in order further to reduce crosstalk by increasing the spacing between pairs, the number of pairs on a crossarm has been limited to four instead of five, omitting the pole pair. Now, by applying a new transposition system designed for type *J* operation up to 140,000 cycles, an eight-inch-spaced four-crossarm line may be arranged to transmit type *J* frequencies on at least 10 pairs out of 16. Type *C* systems may, of course, be used on all of the pairs. Finally by using the most advanced transposition design methods, and increasing the crossarm spacing, in addition to the features noted above, a new line may be constructed to permit the operation of 16 channels on all pairs.

To make the pairs of wires good for type *J* systems, more than a fourfold increase in frequency range, was difficult. The natural tendency of the circuits to crosstalk is increased even more than the frequency ratio, so that in addition to applying a new transposition design it is necessary that the transposition poles be more accurately located, and that the sags of the two wires of each pair be kept more nearly alike. On lines which already have eight-inch-spaced wires, no major structural changes are necessary. However, on lines which have only 12-inch-spaced wires and where it is desired to make available a number of pairs for type *J* transmission, structural changes, such as respacing the wires of the pairs concerned to 6 inches, are necessary in order to reduce the coupling.

One factor of extreme importance is that of reflected near-end crosstalk. In the application of transposition systems it is usually not possible to reduce the near-end crosstalk to a magnitude approximating the far-end crosstalk. It is the latter with which the carrier systems are chiefly concerned, since similar types of systems on different pairs all transmit the same frequency range in the same direction. If, however, the lines concerned do not have smooth impedance characteristics, that is, a high degree of freedom from reflection effects, near-end crosstalk may be converted by reflection into far-end

crosstalk of sufficient magnitude to be controlling over the true far-end crosstalk.

This means that lines to be used for several type *J* systems must be made unusually smooth electrically—impedance variations kept within a few per cent. The achievement of such smoothness consists chiefly in:

1. Reducing the electromagnetic and electrostatic couplings to other pairs so that there are no large energy interactions, with corresponding impedance irregularities. Generally speaking, when the pairs concerned have been transposed for reduced far-end crosstalk up to the maximum frequency transmitted, this condition is also satisfied.

2. Minimizing the effect of intermediate and terminal cables. This latter problem has caused considerable concern and is responsible for the development of several new techniques in the design and treatment of such cables, where they appear in a long line otherwise consisting chiefly of open wire.

Cable Treatment

As a means of overcoming the reflection and attenuation effects of short pieces of terminal or intermediate cable, loading naturally suggests itself, as applied in type *C* systems, where the cable pairs involved are commonly equipped with carrier loading coils, spaced at about 700-foot intervals. This compares with the 3,000-foot or 6,000-foot spacings which are standard for voice-frequency loading. However, loading pairs in existing cables satisfactorily up to 140,000 cycles would mean coils at approximately 200-foot intervals. Because of physical limitations, existing manhole spacings, etc., this is highly impractical. A reasonable solution has, however, been found in the creation of a new form of low-capacitance high-frequency cable—a disk-insulated unit which has constructional features in common with the coaxial cables and a capacitance of only 0.025 microfarad per mile as compared with about 0.062 microfarad for conventional cable pairs. This permits more practical loading-coil spacings. These disk-insulated units are made up as spiral-fours, that is, two pairs (0.051 inch diameter wire) which form the diagonals of a square. When these cables are loaded with small coils at intervals of approximately 600 feet, they present impedance characteristics substantially equivalent to that of an open-wire pair over the desired frequency range. Accordingly, they form a desirable, although somewhat expensive, solution of the problem of intermediate or entrance cables. As shown in figure 6, the spiral-four units are bound together in complements of seven or less under a lead cable sheath similar to standard toll cables. It should be noted that the low-capacity disk-insulated loaded cables not only provide a satisfactory solution of the impedance matching problem, but they also give a cable circuit of low attenuation—approximately 1.2 decibels per mile at 140 kilocycles.

Nevertheless, where spare pairs exist in cables, it has often been found economical to use them for type *J* transmission. It is possible to use them only nonloaded, in which case the attenuation is very high—four to six decibels per mile, depending on the gauge, at 140 kilocycles, and impedance matching transformers are, of course, required at the junction of the open-wire and cable. There are cases where this higher attenuation may be permitted and these pairs are used by separating the type *J* range from the lower frequency range, which is transmitted through pairs equipped with the older type *C* carrier loading. The separation is accomplished by filters which are usually housed in small filter huts at the junction of the open-wire line and cable.

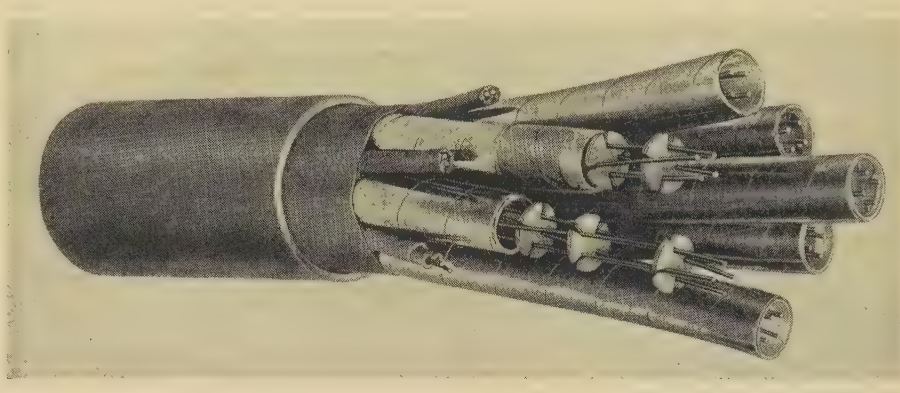


Figure 6. Disk-insulated cable

Sheath diameter 2.3 inches

In other cases it has been found economical to use the frequency-separation method with filters and to install new nonloaded cables of lower attenuation to lead in the type *J* frequency band alone. Paper-insulated ten-gauge pairs or the disk-insulated spiral-four cable of the type described above may be used for this purpose. In either case transformers are used to match the cable impedance to that of the open-wire line over the type *J* frequency range.

The reflection requirements are so severe and the effects of even short lengths of cable at the high frequencies so serious, that even short lead-in cables, where the open-wire line actually extends to the repeater or terminal building—cables which are only 100 or 200 feet long—must receive special treatment. This has also been accomplished by the use of the disk-insulated spiral-four cables, loaded.

Interaction Crosstalk

Because of the higher attenuation there will be many repeater points on a long line at which the type *J* system will be amplified but at which the other systems and wires on the line will pass through the station without amplification. In this case, even though the type *J* pairs are properly transposed to keep down crosstalk between themselves, there still remains the crosstalk between them and the other pairs on the line, not only pair-to-pair crosstalk but crosstalk from the type *J* pair to various circuit paths consisting of irregular wire combinations.

Two difficulties arise in this case: The first is that the crosstalk from the output of one *J* system into an irregular path may be retransferred into the input of a repeater on another type *J* system. The second is that the crosstalk from the irregular path may be returned to the input of the same repeater and either influence the over-all transmission characteristic or, if sufficiently severe, actually cause the repeater to sing. This general situation has made it necessary to introduce in the circuits at such points "crosstalk suppression" filters in the non-*J* pairs and longitudinal choke coils in all pairs.

Staggering

In addition to the various steps which are taken in order to reduce crosstalk by improving the line conditions, the type *J* system may include a feature which has been used in the type *C* system—the staggering of the channel bands used on

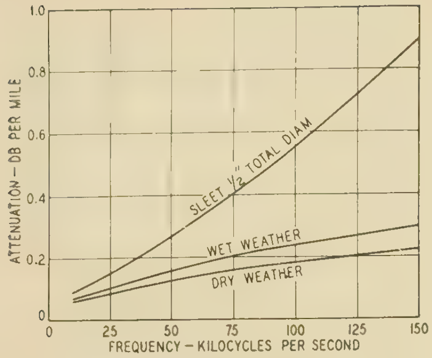


Figure 7. Attenuation-frequency characteristics of open-wire lines

neighboring pairs. The advantage of staggering results from the facts that (*a*) the sensitivity of the ear and the power of the voice vary over the audible range, (*b*) the efficiencies of transmitter and receiver also tend to vary over the frequency range, (*c*) part of a channel band may lie opposite "dead" frequency range on an adjacent pair, and (*d*) by controlling the arrangement of the sidebands the crosstalk may be made unintelligible even if not inaudible. The staggering feature is readily provided in the type *J* system by a suitable choice of carrier frequency for the second group modulator and first group demodulator. With the staggered systems the highest frequency used would be about 143 kilccycles.

Attenuation Problem

In what has preceded in the discussion of line problems, the emphasis has been confined chiefly to the question of the smoothness of a line from an impedance standpoint in order to keep down reflection effects and, correspondingly, to improve the operation from a system-to-system crosstalk standpoint. There is also the problem of the higher attenuation incident to the use of higher frequencies. Between 30,000 cycles and 140,000 cycles, the normal wet-weather attenuation for a 165-mil open-wire pair, for example, rises from about 0.13 to 0.28 decibel per mile—an increase of approximately 2:1. Repeaters on the type *J* system, if applied on the basis of approximately the same output level and minimum level requirements, must be spaced at about one-half the interval of the type *C* systems. Normal spacings for type *J* systems would therefore be expected to range from 75 to perhaps 100 miles where no large amount of intermediate cable existed.

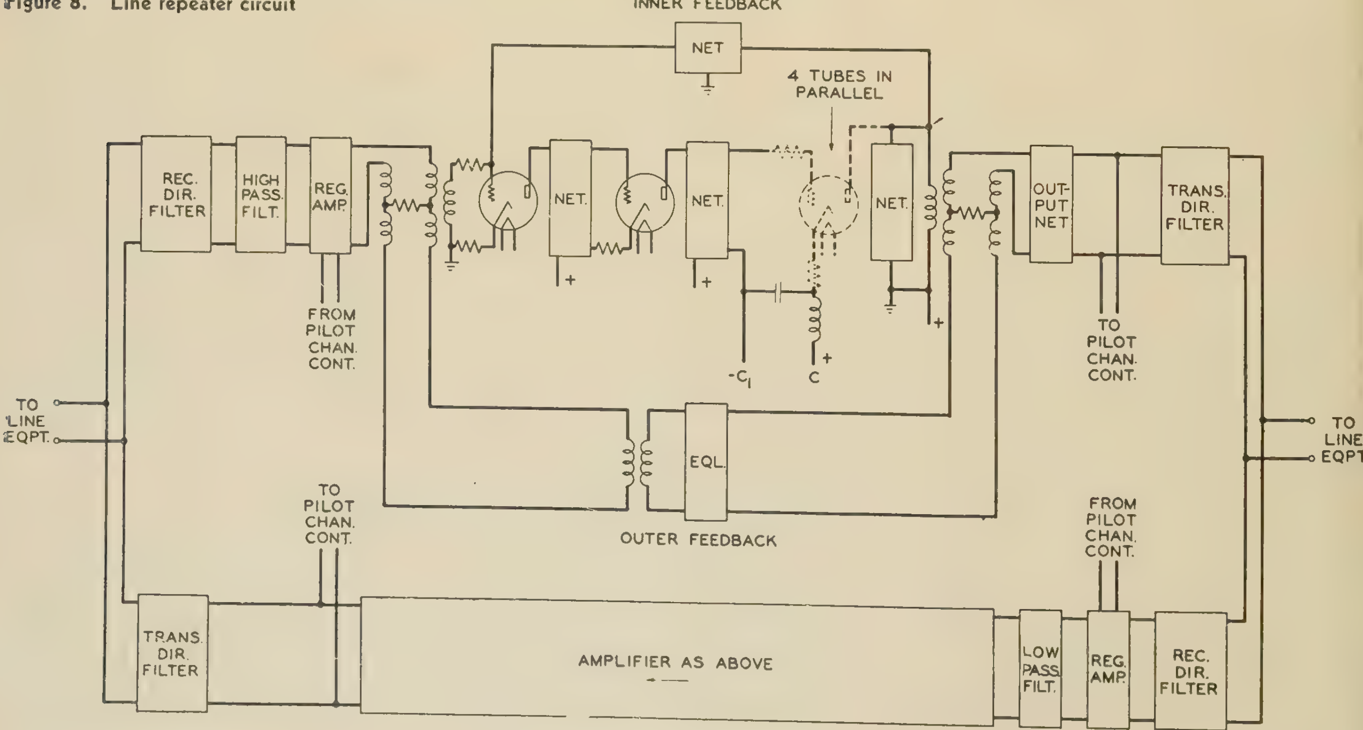
However, another problem, not present to a similar degree at the lower frequencies, tends in many cases to have a controlling effect on this spacing, that is, sleet or ice on the wires. With ice, frost, or snow on the wires, the wet-weather attenuation may be exceeded by very large amounts. The additional attenuation is due primarily to the coating on the wires themselves rather than the coating on the insulators. It arises from the potential gradient through the ice deposit in combination with the high dielectric loss characteristic of the ice or snow coating. Figure 7 gives examples of the attenuation frequency characteristics of open-wire lines, including certain measurements with ice coating. The exact increase in attenuation due to snow and ice naturally depends on the thickness and other characteristics of the coating. Even very thin coatings of ice on the wires tend to raise the attenuation at 140 kilocycles from the normal wet-weather figure of about 0.28 decibel to about 1 decibel a mile, that is, an increase of three or four to one Extremes up to five decibels per mile have been measured for short lengths of line with ice nearly two inches in diameter. Such heavy ice obviously approaches the mechanical breakdown conditions for the line.

Where ice and sleet occur the repeater spacings may be reduced to about 50 miles or less. The repeaters now being provided for the type *J* systems have gains of approximately 45 decibels. Repeaters are under development which are expected to raise the maximum available gain to something like 75 decibels. The normal dry or wet weather operation of such repeaters would be limited to gains of perhaps 10 to 25 decibels depending upon the amounts of cable included. The problem of obtaining automatic gain control over the extra wide range required by the high sleet attenuations is a difficult one.

Repeaters

At each repeater point line filters and directional filters are required on both sides of the amplifying equipment to separate type *J* currents from those of

Figure 8. Line repeater circuit



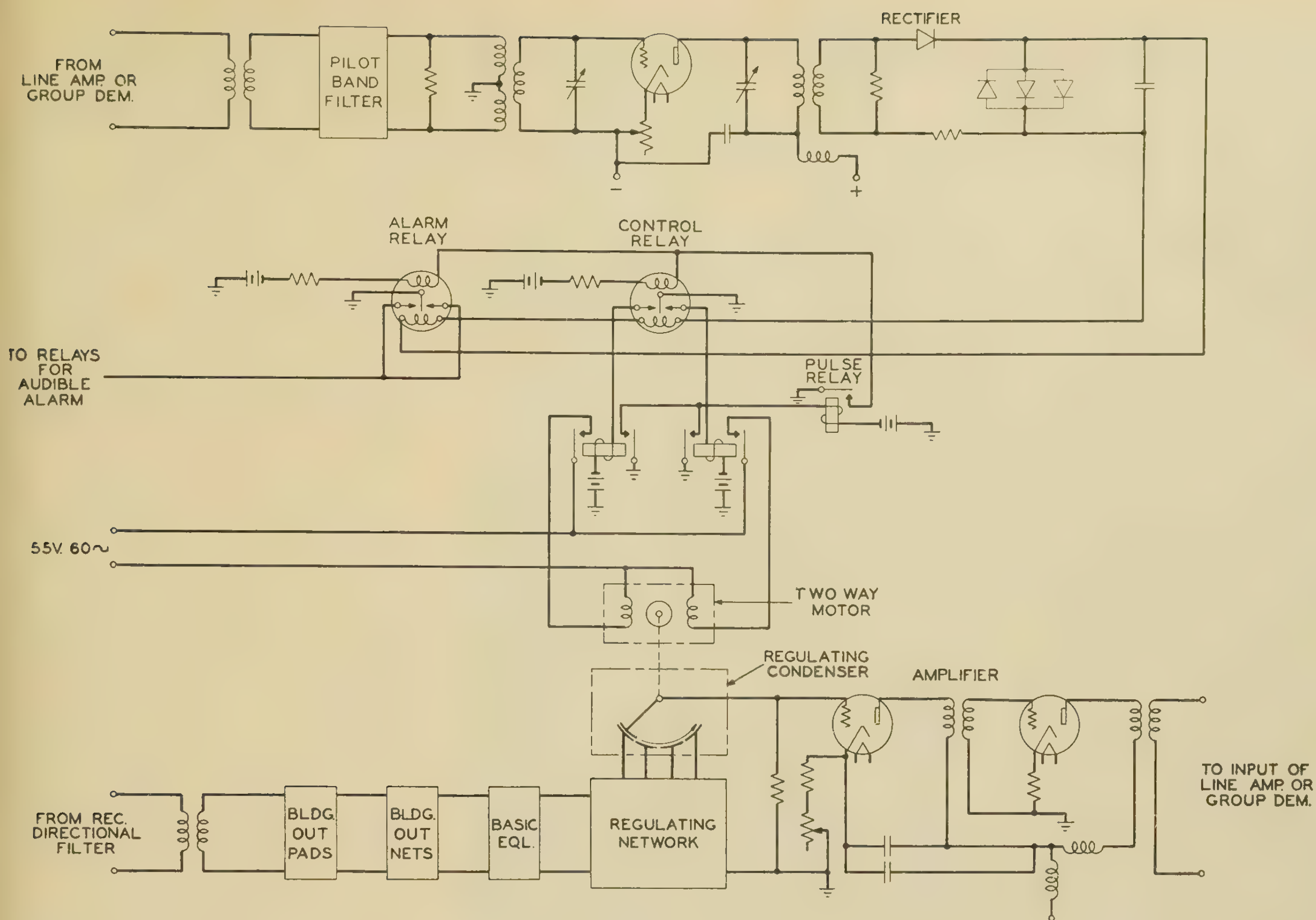


Figure 9. Regulating amplifier and pilot control

lower frequency services on the line and to separate oppositely directed groups for separate amplification in one-way line amplifiers. These filters have been described in connection with the terminals where they perform similar functions. Two regulating amplifiers, one for each direction of transmission, properly controlled to compensate for variations in the attenuation of the preceding line section, are also needed at each repeater point. These are described later under "Regulation."

Figure 8 shows the circuit of one of the line repeaters and indicates the location of the directional filters, and certain supplementary filters for suppressing frequencies outside the transmitted range; also the regulating amplifier circuit, and the pick-off of the pilot channel which controls the gain.

The line amplifier has three stages of pentodes. The first two stages use single tubes of high voltage amplification and low power capacity while the third stage has four power pentodes in parallel to increase the output capacity. Because of considerable heat developed by these power tubes, special precautions are necessary to dissipate the heat and to protect capacitors and other elements mounted near them.

Negative feedback to improve the operation of the amplifier is supplied over two paths. The inner feedback, from the plates of the output tubes over a properly designed network to the grid of the input tube, reduces the gain at frequencies outside the transmitted band and so prevents singing at those frequencies. It has little effect at frequencies within the type *J* range. The outer feedback path includes the input and output transformers, which are made as hybrid coils. In each of these one pair of the conjugate windings is connected to the incoming or outgoing circuit of the amplifier while the other pair is used for the feedback connection. By feeding back through the transformers in this way, they benefit by feedback in much the same way as the tubes, and the over-all characteristic of the amplifier is practically independent of the transformer characteristics. This feedback reduces the amplifier gain by over 40 decibels and correspondingly reduces modulation effects within the amplifier, and gives exceptionally stable transmission with respect to tube and voltage changes. It is also designed to improve and stabilize the input and output impedances.

Equalization

Equalization is necessary in each direction of transmission at a repeater point and in the receiving direction at a terminal, to compensate for frequency distortion produced by the preceding section of line. Fortunately, the attenuation-frequency curves for the usual open-wire circuits, that is, 104-, 128-, and 165-mil wire, have nearly the same shapes for section lengths giving the same attenuation at the maximum frequencies for the two directions of transmission, so that these various circuits can be equalized alike.

As is well known, the transmission frequency characteristic of an amplifier with large feedback is almost the inverse of that of the feedback circuit itself, so that the insertion in the feedback circuit of a network having the same characteristics as a line section will provide equalized transmission over the amplifier and section combined. In the outer feedback circuit of the line repeater is included an equalizer which has a characteristic sloping with respect to frequency in the same way as the variation

Figure 10. Typical installations

A—Auxiliary repeater station

B—Cable hut

C and *D*—Terminal installations

in loss under wet weather conditions of the longest open-wire section likely to be used. Thus, there is provided in the repeater a basic equalization for this longest wet-weather line. At a receiving terminal a basic equalizer is provided which performs this same compensation, but in this case the slope of the curve must necessarily be opposite to that of the line attenuation and of the equalizer in the feedback path of the line repeater.

Line sections, however, vary in length and in the amount of entrance cable included. In order that they may be properly corrected by this basic equalization, they must be built out to equal this longest wet-weather section. For this purpose there are provided flat loss pads and building-out networks whose losses have the same frequency shapes as the losses of short lengths of open-wire circuit. These pads and networks can be inserted or omitted by simple changes in strapping. They suffice to build out the shortest section which is expected to be used.

Pilot Currents

For a satisfactory system, arrangements must be provided to correct automatically for the effects on line attenuation due to changes in weather, by adjusting the amplification at each repeater point and in the receiving terminal circuit. To permit measuring these effects a pilot current of fixed frequency, near the middle of the transmitted band, and of constant amplitude, is supplied from each terminal. This is applied to the transmitting side of the terminal circuit between the 12-channel terminal and the first group modulator, where the message band lies between 60 and 108 kilocycles. The pilot frequency is 84.1 kilocycles which is obtained by modulation of 88 kilocycles, from one of the output taps of the channel supply of that frequency, with 3.9 kilocycles derived from a tuning fork oscillator. This modulation is performed in a copper-oxide bridge similar to the channel modulators and the desired product is selected by an 84-kilocycle carrier supply filter. The output of 84.1 kilocycles is sufficient to supply pilot current for ten terminals in the office. A sharply selective crystal band elimination filter is inserted between the output of the 12-channel terminal and the point where the pilot source is bridged on the circuit to eliminate any current near the pilot frequency which would interfere with the small pilot current that is sent out to control the system.

The two group modulation processes alter this pilot frequency of 84.1 kilocycles so that it appears on the line as 59.9 kilocycles in the west to east directional band, and as 116.1 kilocycles in the east to west band. Correction in accordance with the magnitudes of these mid-group currents in the two directions is satisfactory over all 12 channels under ordinary conditions. In the case of ice or snow the channels at the edges of the directional frequency groups may not be properly adjusted. Additional pilot frequencies will probably be needed ultimately to care for such unusual conditions.

Regulating Amplifier

Figure 9 shows the circuit of the regulating amplifier and above this, the circuit of the pilot-channel receiving equipment which controls it. Current enters the regulating amplifier circuit from the left, coming from the receiving directional filter through a shielded transformer and the pads and building-out networks used for equalization. At the terminals the circuit includes also the basic equalizer. Last in the circuit leading from the line to the regulating amplifier is the regulating network which consists of a series of three equal networks having a total loss of 20 decibels at 140 kilocycles in the east to west direction and 15 decibels at 84 kilocycles in the west to east direction. The network loss increases with frequency in the same way as the difference between dry and wet weather attenuation of the line. The two terminals of the regulating network and the two junction points between the three networks are brought to four sets of stator plates on an adjustable capacitor. The rotor of this capacitor, which has about the same area as one set of stator plates, is connected to the grid in the first stage of the regulating amplifier. Rotation of the capacitor therefore applies, to the grid of the first tube, a volt-

age which decreases continuously as the capacitor rotates from left to right.

The regulating amplifier has two stages of pentode tubes, a high input impedance necessary for the proper operation of the capacitor potentiometer, and feedback to stabilize the gain and to prevent intermodulation of the channels. Its output goes to the line amplifier at repeater stations, and to the first group demodulator at the terminals. At a west terminal there is interposed a high cut-off filter to eliminate frequencies above the upper band which may have been picked up on the open-wire line.

Pilot Control

The setting of the capacitor which controls the regulating network is determined in accordance with the amount of pilot current flowing in the circuit in the direction concerned. At repeater stations the pilot current is picked off at the output of the line amplifier, being separated from the message transmissions by a quartz filter which has about a 30-cycle pass band. For control of transmission from west to east at the repeater stations, this filter selects 59.9 kilocycles and for control of the oppositely directed transmission, 116.1 kilocycles. At the terminals the pilot-channel selecting filter is connected across the output of the auxiliary amplifier following the second group demodulator where the pilot frequency is 84.1 kilocycles. The pilot current from the pick-off filter is amplified in a single-stage amplifier which has feedback for constancy of operation and input and output circuits tuned to the pilot frequency. After amplification the pilot current is rectified by a temperature-compensated copper-oxide rectifier.

The resulting direct current passes through the operating windings of the control and alarm relays. These Weston Sensitrol relays are, in fact, microammeters with high and low contacts made by the pointers. The mechanical bias of the moving system is adjusted so that with the normal pilot current the pointer will remain free in the middle between the two contacts. A change of about 0.5 decibel in this current will cause the pointer of the control relay to make contact with the terminal at the corresponding end of its swing. As the limiting contacts are magnetized and the pointer is of magnetic material, good contact is insured. When contact is made on one side a 60-cycle circuit is closed through the motor which controls the regulating capacitor in such a direction as to cause the loss in the regulating network to be increased. Closure of the other contact similarly causes the loss in the regulating network to be decreased. Closure of either contact also closes a circuit containing a slow operate "pulse" relay to release the Sensitrol relays after an interval of about four seconds During this time the gain of the regulating amplifier will have been changed about 0.1 decibel. If now the pilot current level is within 0.5 decibel of normal the operation is complete. If not, it is repeated and the device keeps periodically testing the circuit so long as it is away from satisfactory compensation. There are also alarm circuits for attracting attention in cases of wide variations in equivalent. In severe ice conditions where a single regulating repeater has not sufficient gain to make up for the great loss in the line, the next succeeding repeater will do its utmost to make up the deficiency.

Conclusion

In what has preceded, developments have been described which are making it possible to provide a very substantial increase in circuits on open-wire pole lines without additional wire stringing. Illustrations of typical office installations of type *J* carrier equipment, unattended repeater stations, and filter huts are shown in figure 10.

Three stages in the development of the open-wire line over the past 20 years, giving successive increases in circuit capacity, are shown in figure 11. Prior to the application of carrier systems, a four-crossarm pole line would yield 30 voice circuits. Now, on a new line 256 circuits are potentially obtainable. Thus it is probable that the open-wire line will continue as an important factor in furnishing facilities in moderate numbers, particularly in the less densely populated sections of the country and where climatic conditions are not unfavorable. Installations of type *J* systems have already been made in various parts of the United States.

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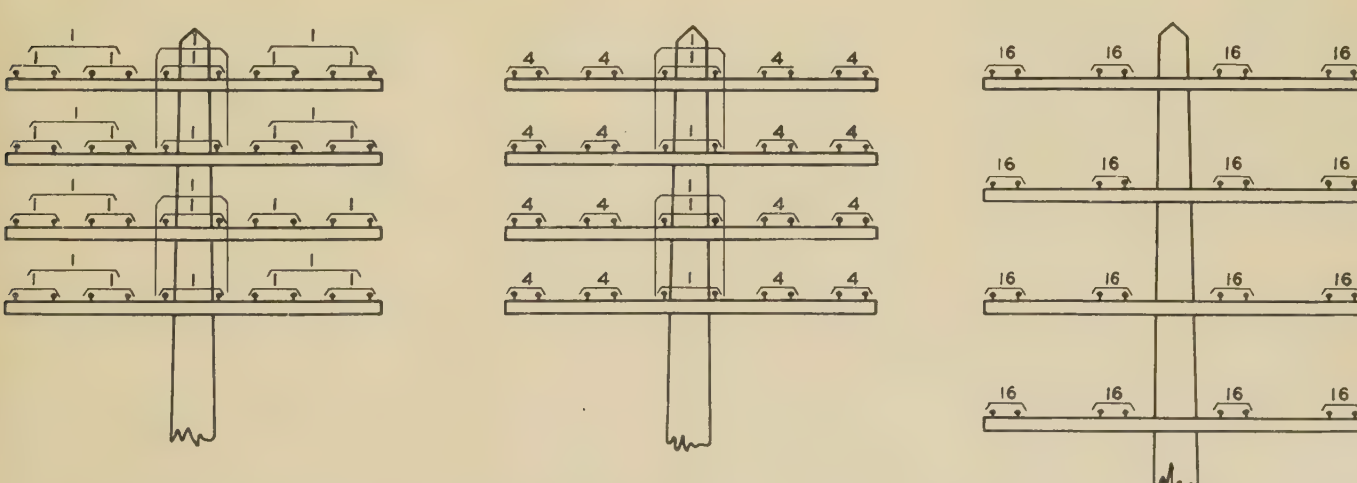

Facilities—30 voice circuits

Line construction with eight-inch-spaced nonphantomed nonpole pairs. Type *C* systems on all eight-inch-spaced pairs

Facilities—22 voice circuits, 48 carrier circuits, total 70

New line construction; all eight-inch-spaced pairs; no pole pairs; crossarms spaced 36 inches instead of 24 inches; no phantoms

Facilities—16 voice circuits, 240 carrier circuits, total 256

Figure 11. Growth in line carrying capacity

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Discussion

James J. Pilliod (American Telephone and Telegraph Company, New York, N. Y.): The 12-channel open-wire carrier telephone systems came into commercial use in the plant of the long lines department of the American Telephone and Telegraph Company during the latter part of 1938 and in January of this year. Six such systems have been placed in operation, one in Texas between Dallas and Houston, two on the recently completed transcontinental route between Oklahoma City and Whitewater, one between Oklahoma City and Albuquerque, and two between Charlotte and West Palm Beach. These systems provide 72 channels, making a total of nearly 55,000 miles of telephone circuit.

The longest of these systems is the Oklahoma City–Whitewater (Los Angeles) whose route totals approximately 1,180 miles and in which there are 16 intermediate repeater points. The spacings of these repeaters were adjusted in accordance with knowledge of abnormal weather conditions. On the eastern end, between Oklahoma City and the Texas–New Mexico state line, in which sleet is to be expected, the average spacing is in the order of 50 miles, while west of this point where less severe conditions, from a transmission standpoint, are encountered, the spacing averages more in the order of 75 miles. On the southern systems spacings ranging from 75 to nearly 100 miles obtain.

Experience with these systems thus far has been very satisfactory. Excellent telephone circuits have been obtained and the channels, for the most part, have been extended by other types of facilities. For example, four-wire cable circuits out of Oklahoma City to Chicago, St. Louis, and New York, and out of Charlotte to Washington and New York, the combined facilities forming very long toll circuits over which excellent transmission is obtained.

It may be of interest to note that many of the channels between Charlotte and West Palm Beach are extended north to New York by means of the cable carrier systems between New York and Charlotte of the kind described in a paper by C. W. Green and E. I. Green, presented at the winter convention in January 1938, to form New York–West Palm Beach and New York–Miami circuits.

The use of this type of facility will doubtless be an increasing factor in our plans for circuit growth.

R. M. Goetchius (nonmember; American Telephone and Telegraph Company, New York, N. Y.): In addition to the systems mentioned by Mr. Pilliod, two additional type *J* systems were placed in service in 1938, one between Dallas and San Antonio, a distance of 280 miles, and one between Dallas and Longview, a distance of 130 miles. These two systems provided 5,000 miles of additional telephone circuit. In the application to existing telephone plant of all these type *J* carrier telephone systems, there are many engineering problems. For example, in laying out the intermediate repeater stations, consideration must be given to the probability of ice formations, static noise, which is a maximum in the summer, and possible interference from near-by radio stations. In many cases portable testing apparatus is used to make field measurements of the order of magnitude of these effects in the particular location.

After the number of auxiliary repeater stations between existing offices has been determined on these general considerations, it is necessary to make a detailed survey to determine suitable sites for the repeater office. Important elements in these considerations are the availability of commercial power and the accessibility to main roads. In most parts of the country commercial power is readily available within a few miles of the theoretically best location for a repeater station. However, in the case of several offices on the route between Oklahoma City and Whitewater, Calif., no source of power was available at the repeater site or even within a reasonable distance. Accordingly, windmill generators were installed together with emergency gas-engine generators which served as a source of power for charging the batteries used at these locations.

An important problem which must be considered in applying type *J* systems to an existing open-wire line is the extent to which the transposition system and configuration of the line needs to be modified to make it satisfactory for type *J* use. The solution in each case depends to a large extent on how well the line is transposed and on the number of type *J* systems for which it appears reasonable to arrange the line. On the present-day eight-inch-spaced well transposed lines, it is possible to apply a number of type *J* systems with relatively small changes in the present arrangements. On the older lines employing 12-inch spacing where only a limited number of crossarms are now available for type *C* carrier, the application of type *J* systems may require extensive retransposing and even respacing to six inches of the pairs which are used for type *J* operation. The solution for this problem which was worked out in Texas for the Dallas–Houston and Dallas–San Antonio systems was quite different from either of the general treatments mentioned previously. On both of these routes, which consisted of five full crossarms of wire, it was decided to provide additional facilities during 1937 before the type *J* system was available. In order to prepare the additional wire for future type *J* application and to eliminate the possible hazard due to wires from the upper crossarm falling on the type *J* wire, an additional crossarm was placed on both these lines 24 inches above the present top crossarm. This was accomplished by adding a simple extension fixture consisting of a four-inch steel H-beam arranged to be fastened to the pole by the through bolts supporting the two upper crossarms. On this additional crossarm there were placed four six-inch-spaced 128-mil conductors suitably transposed for type *J* operation.

While experience with commercial operation of the type *J* systems has been quite limited, the service results to date have been very good. The circuits themselves are very quiet and transmit a wider band of frequencies than previous types of open-wire carrier systems. The regulation features of the system automatically compensate for all normal weather changes and therefore minimize the amount of periodic maintenance work required to maintain satisfactory operation. As an example of this, during the first three months of operation of the Dallas–San Antonio system, there was not a single report of trouble to the testboard on any of the circuits operated over this system. Furthermore, if trouble conditions arise in the system, automatic alarm features notify the maintenance attendants and, in some cases, indicate the nature of the trouble so that remedial measures may be quickly initiated.

An Electronic Control Circuit for Resistance Welders

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FLEXIBLE electronic control circuits for the precise timing of resistance welders have been utilized thus far primarily on large welders. This paper describes a new circuit using recently developed types of cold-cathode electronic tubes which makes the application of such control economically practicable to small as well as large welders. The advantages of precise timing of welds by electronic means are well recognized, particularly for exact duplication of welds and for fabrication of materials difficult to weld or subject to physical or chemical changes if overheated. Utilization of the method on small laboratory or industrial welders may render expedient processes that heretofore were considered unavailable except for large-scale production.

Elements of Operation

In the circuit shown by the heavy lines near the top of figure 1, two band-ignitor mercury-arc tubes[1] are connected in a parallel-inverse relationship in series with the welder-transformer primary winding to conduct alternate half-cycles of current. These tubes consist of a mercury-pool cathode and a metal anode in an evacuated bulb with a metal band located outside the tube near the edge of the surface of the mercury. Conduction begins in such tubes upon application of a high-voltage from a spark coil to the external starting band if the anode is more than about 50 volts positive with respect to the cathode.* The function of the control unit, which constitutes the remainder of the circuit, is to initiate conduction in the band-ignitor tubes whenever the control switch S is moved from point 2 to point 1. Conduction is to begin precisely at a chosen phase angle in the supply-voltage alternation during an arbitrarily predetermined number of half cycles. The band-ignitor tubes then act as electrically controlled synchronously operating switches in series with the welder. Two are necessary to conduct the current in opposite directions because of their unilateral-conduction or rectifying characteristic. The threefold requirements of the control unit are that it (1) deliver a controllable number of high-voltage pulses to the starting bands of the band-ignitor tubes, (2) synchronize these pulses with the supply-voltage alternation at a controllable phase angle, and (3) provide operation which is independent of both the time at which the control switch S is operated and the interval it is held closed.

Operation in Detail

The Strobotron tube T_2 is the nucleus of the control circuit. Its function is to complete a discharge circuit for capacitor C_4 through the primary winding of the spark coil TR_2 and thereby to impress a high-voltage surge upon the starting bands of the band-ignitor tubes. Since the Strobotron tube is a relatively recent development, its characteristics as utilized in this application will be summarized briefly.

The electrodes of the Strobotron tube[2] consist of a cathode composed of a caesium compound, a metal anode, and two grids located between them. The tube is filled with neon gas at a pressure of about 1.5 millimeters of mercury. A glow discharge will start between the grids if the voltage between them reaches an amount of the order of 100 volts, with one grid positive and the other negative with respect to the cathode. If the anode is made 200 to 400 volts positive, the discharge will immediately transfer to the anode and cathode, and it will change from a glow into an arc with a low voltage drop if the source of supply for the anode-cathode circuit is capable of furnishing several amperes even momentarily. Less current to the grids is required for starting if the inner grid (the one nearer the cathode) is made negative and the outer grid (the one nearer the anode) is made positive than if the grids have the reversed polarities.

It is necessary that the high-voltage pulse to the starting bands occur twice during each cycle of the supply voltage in order that both band-ignitor tubes fire and conduct the alternate half cycles. To this end, the output of a peaking[3] transformer TR_1 is rectified by the full-wave rectifier tube T_1, and the resulting unidirectional peaked voltage wave, having a frequency of 120 cycles per second, is applied to a voltage divider consisting of R_4 and R_5 in series. The major portion of the peaked voltage, about 50 volts in amplitude, is supplied to the inner grid of the Strobotron tube through resistor R_6. The polarity of the voltage is such as to make the inner grid momentarily negative when the peaks occur. Capacitors C_2 and C_3 are included to furnish a surge of grid current and make the firing of the Strobotron tube more positive.[2] In addition they serve to prevent extraneous surges from starting the tube. The primary winding of the peaking transformer TR_1 is supplied through resistor R_2 and capacitor C_1, which form a resistance-capacitance phase-shift circuit for adjustment of the time in the cycle at which the peak of voltage from the secondary winding occurs. This voltage supply to the inner grid of the Strobotron tube is not sufficient alone to cause the tube to fire, but it insures that firing will occur at a frequency of 120 cycles per second when the remainder of the control circuit makes the outer grid positive by an amount greater than about 50 volts. Thus the second of the control-unit requirements listed above is accomplished.

The transformer TR_3 supplies power to heat the cathodes of the various high-vacuum tubes, and in conjunction with the full-wave rectifier tube T_6 it furnishes a direct voltage across capacitor C_7 to operate some of the tubes and to charge capacitor C_4.

Tubes T_4 and T_5 form a "trigger-controlled" time-delay circuit to supply the outer Strobotron grid with a direct voltage for a chosen time interval after the control switch is operated. The adjustable contact on resistor R_{15} is so set that normally grid number 1 of tube T_4 is negative beyond cutoff; hence, the current through R_{11} is negligible and the voltage of point E is practically that of D; namely, about 375 volts positive.

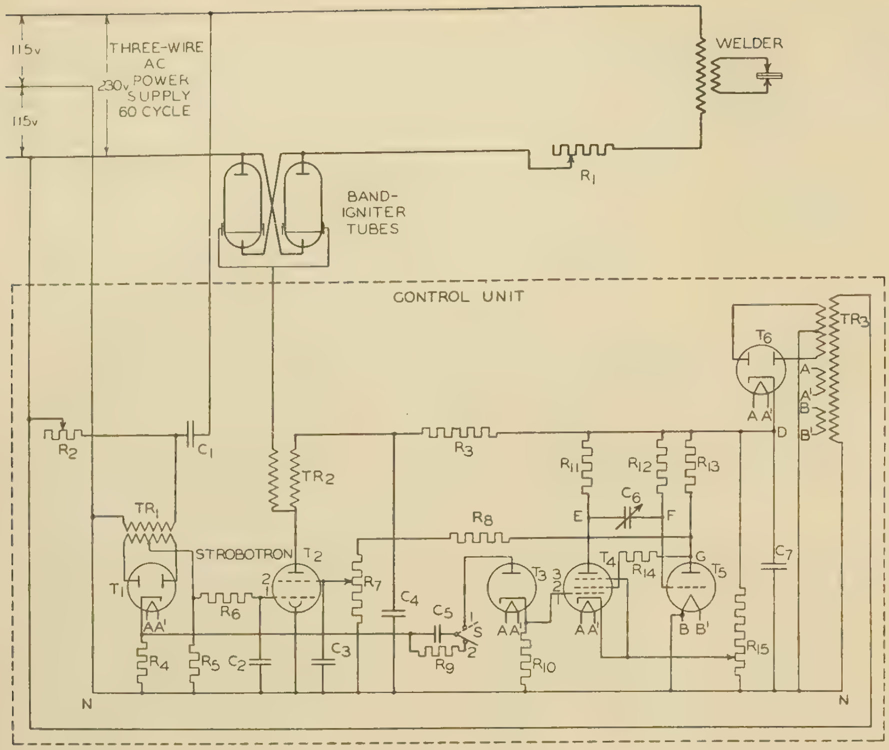
Paper number 39-40, recommended by the AIEE committee on electric welding and subcommittee on electronics, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted September 19, 1938; made available for preprinting December 19, 1938.

T. S. GRAY is assistant professor of electrical engineering, Massachusetts Institute of Technology, Cambridge; J. BREYER, JR., former student at Massachusetts Institute of Technology, is in the engineering department of the Belmont Radio Corporation, Chicago, Ill.

The authors are indebted to H. E. Edgerton for many suggestions in the design of the circuit.

1. For all numbered references, see list at end of paper.

* Voltage polarities of tube electrodes are taken with respect to the cathode as a reference in this article unless otherwise stated.



The grid of tube T_5 is held at a positive voltage through resistor R_{12}; therefore this tube conducts a relatively large plate current, the voltage drop in the plate resistor R_{13} is large, and point G is normally only a few volts positive. The current to the grid of the tube T_5 causes a large voltage drop in R_{12}, making the voltage of point F also only a few volts positive, with the result that capacitor C_6 is charged to about 350 volts, terminal E being positive with respect to terminal F.

When a positive voltage impulse is impressed on grid number 1 of tube T_4, in a manner to be described later, the plate current through resistor R_{11} suddenly rises, the voltage of E falls, and, because the charge on the capacitor C_6 cannot change instantaneously, the grid of tube T_5, point F, is suddenly made highly negative as is indicated in the idealized wave forms of figure 2. The plate current of tube T_5 therefore decreases suddenly. As it decreases, the voltage of point G increases, thereby increasing the screen-grid voltage of tube T_4, which increases the plate current in T_4 and accelerates the changes. The result is that grid number 2 of the Strobotron tube is suddenly made positive, and it remains fixed at this positive voltage until the transient described below is completed.

When point F is driven negative, a charging current flows from D through R_{12}, C_6, and T_4 to N. The voltage of the grid of T_5 therefore approaches that of

Figure 1. Electronic control circuit for a resistance welder

The control unit is enclosed by dotted lines

Circuit constants:

R_1 —	10 ohms	T_5	—Type 01*A*
R_2 —	5,000 ohms	T_6	—Type 6*X*5
R_3 —	3,000 ohms	TR_1	—Peaking transformer
R_4 —	100,000 ohms		
R_5 —	1,000,000 ohms	TR_2	—Automobile-type spark coil
R_6 —	25,000 ohms		
R_7 —	1,000,000 ohms	TR_3	—Plate and filament supply transformer (Thordarson number *T*7020)
R_8 —	1,000,000 ohms		
R_9 —	200 ohms		
R_{10} —	100,000 ohms		
R_{11} —	100,000 ohms		
R_{12} —	1,000,000 ohms	C_1	—1.0 microfarad
R_{13} —	100,000 ohms	C_2	—0.001 microfarad
R_{14} —	50,000 ohms	C_3	—0.005 microfarad
R_{15} —	25,000 ohms	C_4	—1.0 microfarad
T_1	—Type 6*H*6	C_5	—0.005 microfarad
T_2	—Strobotron	C_6	—Decade capacitor
T_3	—Type 1*V*		
T_4	—Type 77	C_7	—24 microfarad

D in a manner that can be represented approximately by an expression involving an exponential with a negative exponent. The grid voltage becomes equal to that of the cathode of tube T_5 in a time of about 0.69 $R_{12}C_6$ seconds, where R is in ohms and C is in farads. After the elapse of this time, tube T_5 begins to conduct again, and the voltage of the screen grid of tube T_4 is thereby decreased. This causes a decrease of the plate current and an increase in the positive voltage of point E, which in turn increases the voltage of point F and accelerates the return of the circuit to its initial condition. The outer grid number 2 of the Strobotron tube T_2 is thus suddenly brought back to a low positive voltage after a time dependent upon the size of capacitor C_6.

The operation of the time-delay circuit described here results in the application of a positive voltage to grid number 2 of the Strobotron tube for a controlled time interval whenever an impulse is delivered to grid number 1 of tube T_4. During this interval the Strobotron will fire at 120-cycles-per-second frequency in accordance with the voltage supplied to its grid number 1, and pulses synchronized with the supply-voltage alternations will be delivered to the starting bands of the band-ignitor tubes.

The first control-unit requirement listed here, namely, that the unit deliver a controllable number of high-voltage pulses to the starting bands of the band-ignitor tubes, is only partially fulfilled by the circuit thus far described. If a control switch were incorporated in the circuit merely to connect grid number 1 of tube T_4 to a source of positive voltage upon manual closure, the number of half cycles of welding current conducted during the controlled time interval would depend upon the time in the cycle at which the control switch was closed, and the welding current would continue as long as the switch was held closed.

These difficulties are avoided by interposing tube T_3 with its associated circuit to deliver only one short impulse to grid number 1 of tube T_4 regardless of the length of time the control switch S is held closed; and to cause this impulse to occur at a particular point in the supply-voltage cycle regardless of when the control switch is closed. Control switch S (actually a relay for remote control) is normally closed on position 2, and operation transfers it to position 1. A small portion (about ten volts) of the next succeeding rectified peak of voltage from transformer TR_1 then appears across resistor R_4 and charges capacitor C_5 through tube T_3 and resistor R_{10}. A small voltage pulse caused by the charging current through R_{10} is delivered to tube T_4 and the controlled weld is thereby initiated. Capacitor C_5 cannot discharge while the control switch is held closed on position 1 because of the rectifier tube T_3. Hence, only one pulse is transmitted to tube T_4 each time the switch is operated, and a number of half cycles of welding current predetermined by the setting of capacitor C_6 occurs.

Controls and Adjustments

The external adjustments on the panel of the control unit, illustrated in figure 3, are the half-cycle adjustment, C_6, which is a decade capacitor, and the conduction-angle control R_2 for adjustment of the starting point of conduction within the half cycles. The decade capacitor provides for any number of half cycles from one to ten, and the range may be extended to much larger numbers of half cycles by the addition of an external supplementary capacitor in parallel with C_6. The conduction-angle control permits delay of the point of ignition of the band-ignitor tubes in each half-cycle of supply voltage beyond the natural lagging phase angle of the welding-load current. This delay makes possible the elimination of a high transient surge of magnetizing current,[4] and a reduction in crest current.[5] It insures exact duplication of successive welds and gives smooth, continuous control of the welding heat. In all, four methods of controlling the welding heat are provided, namely, by adjustment of (1) the number of half cycles, (2) the firing point within the half cycles, (3) the resistor R_1 in series with the welder-transformer primary winding (practicable only with small welders), and (4) the taps on the welder-transformer windings.

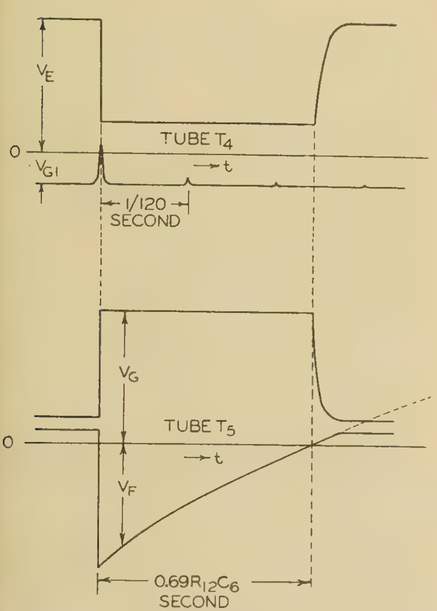


Figure 2. Approximate wave forms of the voltages in the time-delay circuit

Voltages are for the lettered points in the circuit, and are measured from the cathode of the corresponding tube

Two internal adjustments are included in the control unit to provide for differences in replacement tubes. These are the voltage adjustment R_7 for the grid number 2 of the Strobotron tube, and the grid-bias adjustment R_{15} for the tube T_4. The adjustable contact on R_{15} is set by advancing it upward on the diagram slightly beyond the point at which the time-delay circuit becomes stable and ceases to oscillate continuously with the control switch S in its normal position number 2. The setting of the adjustable contact on resistor R_7 is made by advancing it upward on the diagram slightly beyond the point at which the Strobotron first fires when the control switch is moved to position number 1. Once made, these adjustments should not have to be changed.

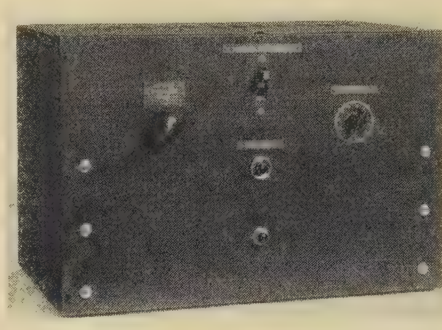
Performance

A desirable feature of the resistance-welder control circuit is that the main tubes in it are of the cold-cathode type and do not require heating power. As a result, the Strobotron and the band-ignitor tubes are ready for service instantly without the time-delay requirement for cathode heating necessary with some control tubes. The only time delay in starting the unit is that inherent in the cathodes of the other tubes, which are of the radio-receiver type and require a time comparable to that of the ordinary radio receiver for heating. When the main switch is closed to excite the whole circuit, the cathode of the triode T_5 in the time-delay circuit must reach its thermionic emitting temperature before that of the rectifier tube T_6; otherwise grid number 2 of the Strobotron tube will receive a positive voltage, and welding-current cycles will continue steadily for a few seconds. For this reason a quick-heating filament-type tube, the 01*A*, is included in the circuit.

The calibration of the decade capacitor C_6 is practically independent of manufacturing variations in tubes T_4 and T_5 because the resistors R_{11}, R_{12}, R_{13}, and R_{14} are large compared with the effective resistances of the tubes while they are conducting. Thus it is not necessary to recalibrate the capacitor when tubes are changed.

The life of the Strobotron and the band-ignitor tubes depends primarily on the number of welds made, and is independent of the standby intervals between welds during which the unit is left connected to the power supply. These cold-cathode tubes are of simple and rugged construction, and laboratory tests indicate their life to be adequate to render them practical for spot-weld service.

The control unit may be assembled in a small cabinet no larger than a table-model radiobroadcast receiver, for all the tubes and circuits in it are of low power ratings. The unit is sufficiently powerful to control the band-ignitor tubes for a welder of any size. It is also suitable for the control of ignitron-type tubes if the spark coil TR_2 is replaced by an appropriate transformer. The model illustrated in figure 3 has been used thus far to operate a small welder of one-kilovolt-ampere rating with current crests of 300



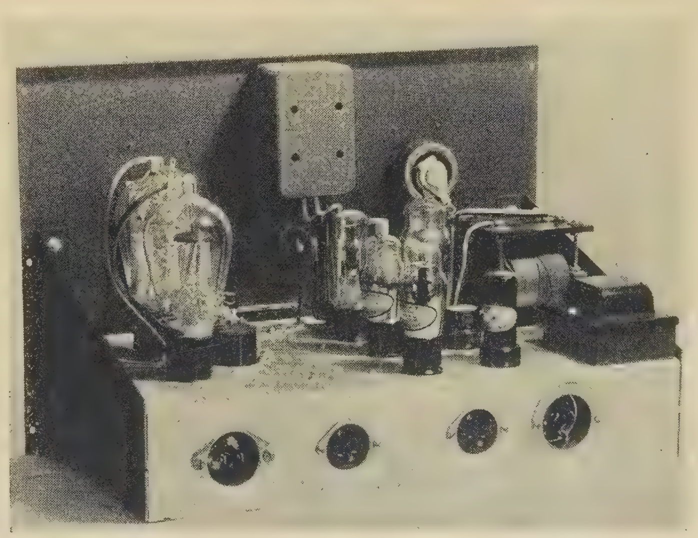


Figure 3. External and internal views of the resistance-welder control-unit assembly

amperes, but there appears to be no inherent limitation to its use with larger welders if band-ignitor tubes of sufficiently high ratings are substituted in the circuit.

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1. **T. S. Gray and W. B. Nottingham,** *Review of Scientific Instruments,* volume 8, February 1937, pages 65–8.

2. **A. B. White, W. B. Nottingham, H. E. Edgerton, and K. J. Germeshausen,** *Electronics,* volume 10, March 1937, pages 18–21.

3. **O. Kiltie,** Electrical Engineering, volume 51, November 1932, page 802.

4. **R. N. Stoddard,** Electrical Engineering, volume 53, October 1934, page 1366.

5. **J. W. Dawson,** Electrical Engineering, volume 55, December 1936, page 1374.

Discussion

L. G. Levoy (General Electric Co., Schenectady, N. Y.): The authors have presented an interesting description of a control circuit for spot-welding applications. I would like to ask what actual crest voltages are required on the ignitor bands of the power tubes? High voltages, even though of the nature of an impulse of short duration, are frequently a barrier to widespread application, particularly when the same result can be accomplished without the use of high voltage.

The circuit proposed in figure 1 is synchronous starting with respect to phase angle, but is random starting with respect to polarity of the first half cycle of any spot. In a simple spot welder there are no cumulative residual current transients, since the electrodes are lifted from the work after the termination of each spot. There is, however, some residual flux left in the core of the welding transformer. If the spot length and polarity of starting are properly controlled, the effect of this residual flux can be minimized. In a welding transformer liberally designed for the voltage applied, the effects of residual flux are small, but in many cases welding transformers normally run at high flux densities, in which case the effect of residual magnetism may give rise to primary transients and also may result in greater magnetic energy storage at the termination of the spot. This often gives rise to undesirable arcing at the electrodes when they are quickly removed from the work after the cessation of primary current, tending to shorten the electrode life and also cause pitting of the surface of the work. For these reasons, where precise work is to be done it would be desirable to incorporate the feature of unipolar starting in the control circuit. To get the maximum benefit of this, the spot length should be adjustable in full cycle increments only. These features are available on existing electronic controls. Where odd half-cycle spot lengths are required, antipolar starting is provided for in available electronic controls. For interrupted spot or seam welding, proper control of the starting polarity and spot length is a requisite to consistent welding.

T. S. Gray: The actual crest voltage required on the ignitor bands of the power tubes, about which Mr. Levoy asks, depends on the thickness of the glass through which the voltage acts. In glass tubes made with ordinary borosilicate glass approximately $1^1/_2$ millimeters thick, the crest voltage required is about ten kilovolts. Thinner glass between the metal ignitor and the mercury pool makes possible a reduction of this crest voltage by a factor of three to five (see K. J. Germeshausen, *Physical Review,* volume 55, Jan. 15, 1939, page 228). There is no serious problem of high-voltage wiring or protection associated with the use of the band-ignitor tubes, because the problem is similar to that in an automobile and the spark coil may be located nearby in the same metal enclosure with them.

The suggestion by Mr. Levoy that for a minimum magnetizing transient the timing should be adjusted in multiples of a full cycle is important, and this adjustment would ordinarily be made except when a single half cycle is desired. It is interesting that provision for unipolar or antipolar starting becomes important with large welders and with interrupted spot or seam welding. In our work with small spot welders we have not observed arcing of the electrodes or pitting of the surface of the work as a result of the lack of such control.

The A-C Arc Progresses

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ELECTRIC WELDING could be pictured very accurately to an electrical engineer as an attempt at maintenance and control of a short circuit. In resistance welding, whether spot, butt, roller seam, flash, or projection, it is an actual short circuit with definite resistance and low voltage drop. The arc is the maintenance of a short circuit in a very unstable medium, generally air, with a comparatively high voltage and nature conspiring to extinguish it. The "holding" of a metal arc is especially difficult as one electrode is continuously melting away into the other electrode called the "work."

In 1930 the AIEE published an article by the writer on time recovery and control of the welding current and voltage, as shown by an oscillograph, with both direct and alternating current. Direct current holds an arc very readily and for the single carbon arc it is so far unchallenged. Alternating current, however, is used for the double carbon arc and for hydrogen-flame double or single tungsten shielded arc welding. It was considered impossible to hold an a-c metallic arc continuously previous to the writer's efforts. The a-c arc, however, once established, is much easier to control and nonferrous alloy welding, even with copper-base alloys, is being accomplished by the use of capacitance and the high-frequency pilot circuit. The introduction of desirable gases, instead of air with its oxygen and nitrogen, to help hold the arc by means of covering on the rods or by surrounding the arc as in "atomic" or "shielded arc" welding, is a very great improvement in the arc stream from the "holding" standpoint. The deposited metal is also greatly improved.

In 1920 the AIEE Journal contained an article by the writer on phenomena of a-c arc welding, also showing a table of electrode arc voltages which is now up-to-date by the general adoption of covered electrodes. If there were ever natural companions for combined good results it is the firm of the a-c arc and covered electrodes.

The a-c arc does not wander or have "arc blow" and the current and voltage can be held ever so much steadier than the d-c arc. Whether for this reason or due to the fact that the alternating current agitates the molten puddle during crystal growth, it is an accepted fact that alternating current and covered electrodes make the highest quality weld deposit.

Class I pressure vessels are welded with alternating current. The $11,000,000 Boulder Dam job was welded with alternating current.

Paper number **39-67**, recommended by the AIEE committee on electric welding, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted April 30, 1938; made available for preprinting December 31, 1938.

Claude J. Holslag is president of the Electric Arc Cutting and Welding Company, Newark, N. J.

Copper alloys—Monel, bronzes, etc.—still give trouble with the metallic a-c arc although the double carbon arc is better for brazing with alternating current than with direct current. Capacitor circuits have brought copper, bronze, and brass alloy welding into the scope of the a-c metallic arc. Previously the d-c carbon arc was the solution for copper-alloy welding and brazing not done by gas welding. The "atomic" or hydrogen-bathed arc has opened a new field for alternating current in that special jobs requiring no contact with the air are extending the field of the art. Copper welding, Monel, stainless, and "tricky" alloys are handled with ease by the hydrogen-flame arc.

The superimposed high-frequency circuit is a very desirable adjunct for special welding purposes. With an air-core transformer and capacitor oscillating circuit (figure 1) high frequency, as used in vaudeville shows, is here made to jump the gap and break down the surfaces for the following of the arc current. This is especially desirable for thin work, stainless, mill-scale plates, and tack welding. It is also useful in starting and maintaining the hydrogen "atomic" arc and in maintaining any arc under specially disadvantageous circumstances such as oil, wind blowing, grease, water, etc. This high frequency can either be superimposed on a-c or d-c arc circuits but is generally used with low-current a-c circuits.

The writer has preached the advantages of covered rods and a-c welding for 26 years. The opposition to covered electrodes and alternating current was tremendous. Even though the AIEE published the writer's 1920 article, the major companies fought acceptance of the truth of the qualities shown very strongly. In fact, acceptance of alternating current only recently has become universal in the United States although in Europe this progress had preceded ours. However, in this country for several years all important work such as class I pressure vessels and oil-fired vessels, penstocks, and those companies who have a testing laboratory use a-c welding for its quality product. In addition to its quality, alternating current is faster and, hence, cheaper in labor, which is 80 per cent of the cost of arc welding. The first cost and the electrical cost of operation is less with alternating current and there is no maintenance. There is no electrolytic action or positive or negative corrosive forces. The deposit is neutral, of finer grain structure, free from porosity, and generally better on all iron and steel work.

Why has a-c arc welding progress been so retarded? The chief sales disadvantage against alternating current has been the open-circuit voltage, which lowers the power factor and is liable to cause nervous shocks especially in wet places.

The writer has lately developed a unique method of lowering the voltage by an odd circuit as shown in figure 2. In its simplest form it consists of a leakage transformer with two windings. One winding is across the operating voltage, say 80 volts, and the other winding is in series with the operating voltage, open-circuited until contact is made. On open circuit the second winding subtracts from the first one leaving the voltage low, say 40 volts. During welding the second winding becomes a reactance, lowering the voltage to the arc voltage of 40. There is no doubt but that this action occurs through phase shifting, exactly as described in the 1920

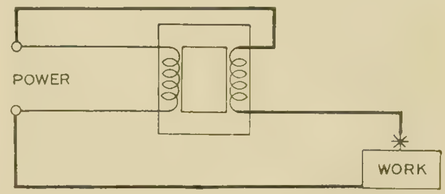


Figure 2. Basic buck-back circuit

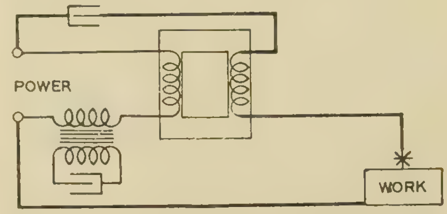


Figure 3. Compensated buck-back circuit

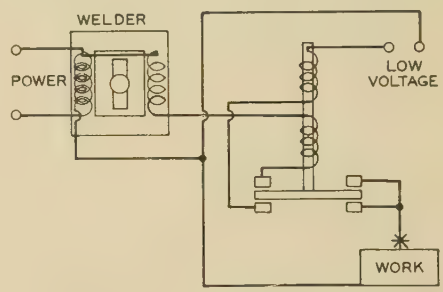
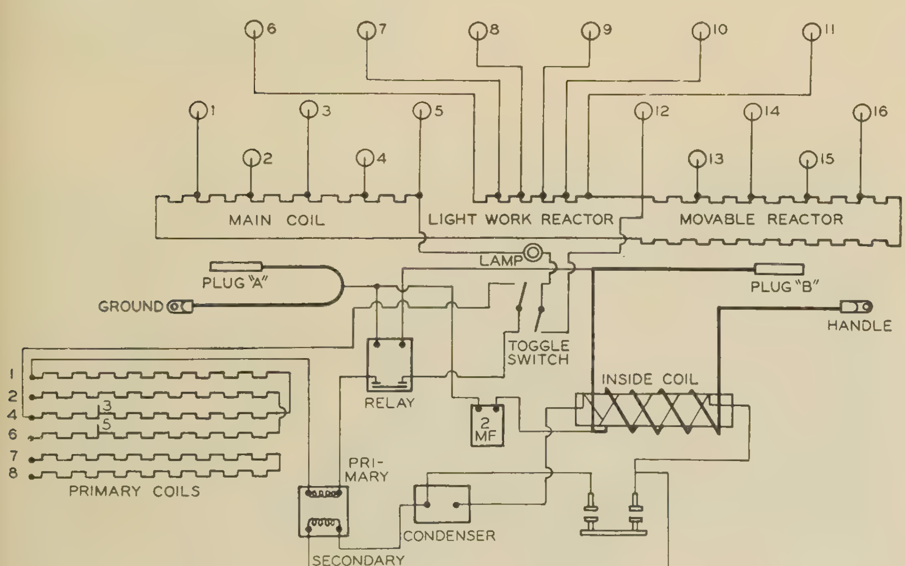


Figure 4. Original low-voltage control circuit

article. In that case, however, the voltages were added. In this case the second winding is used to subtract on open circuit by plain transformer action and under load to subtract by voltage reactance drop and phase shift. Capacitance can be used in this circuit to raise the bucking voltage which disappears under load. Professor Comfort A. Adams and the writer have had a good many months of argumentation as to the interaction of these two circuits and at various current and turn values the action is widely different. The writer admits he cannot explain everything that goes on in this circuit. Perhaps some professor and student will take this up as a thesis. Whether explained or not, it operates as described, and is especially applicable for heavy welding. Figure 3 shows a modified form of this circuit.

The writer has also developed a method of holding the open-circuit voltage low while enjoying the steadiness of applied higher voltage (although this higher

Figure 1. Superimposed high-frequency circuit



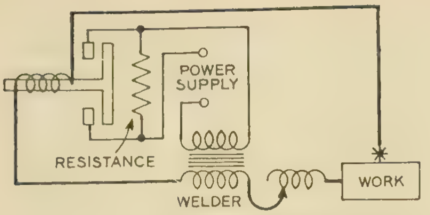


Figure 5. Primary voltage cutdown control

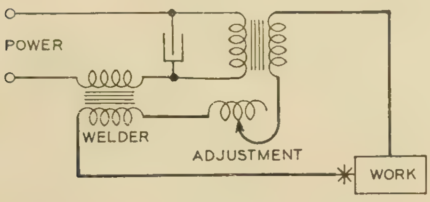


Figure 6. Primary counterbalanced

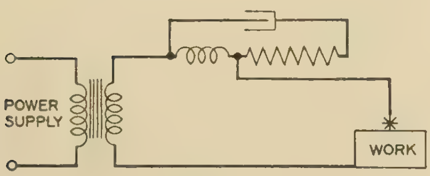


Figure 7. Hunter series circuit

voltage is never present at the arc). Referring to figure 4, the magnetic switch is normally open and only the low voltage of a small (bell-ringing size) transformer at, say, 40 volts is applied to the welding leads. Upon touching the electrode to the work the difference in reactance between the work electrode circuit and the transformer parallel circuit causes the magnetic switch to close the arc circuit, opening the "bringing in" circuit and the switch is held in by a series winding until the arc exceeds, say, 40 volts when it drops out. This system is "positive safe" and thousands are in use, some with the doubtful improvement of a time delay in dropping out. The device is used on large welding transformers in the primary circuit thus cutting off even the core-loss expense and for small arc welders the 40 volts can be obtained from the welder itself by a tap or separate winding.

Another method of accomplishing low voltage, figure 5, also developed by the writer, is to put a resistance or reactance or capacitance in the primary of the arc welding transformer and by a simple series relay cut out this voltage-lowering impediment during welding.

Still another method, figure 6, is to counterbalance a reactance or capacitance in the primary circuit by the welding current but this is limited to a very short range of welding current with insufficient range of adjustment.

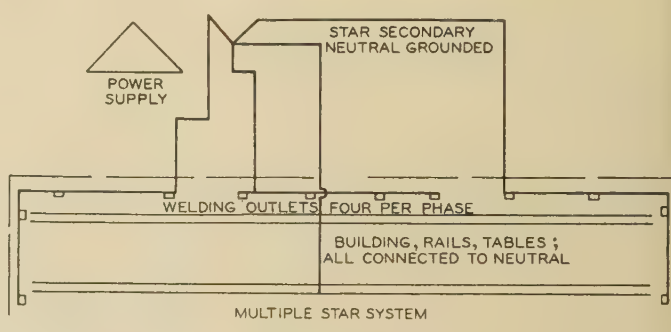
The power factor of a-c arc welding can be overcome with capacitors, which capacitors also give spontaneity of arc, this action being similar to that of condenser coils in an automobile ignition circuit or as in an oil-boiler ignition transformer. Capacitors can also be used to lower the open-circuit voltage by neutralizing the inductive action, as in the Hunter circuit (T. M. Hunter, president of the American Transformer Company), figure 7. Of course, anywhere a capacitor is in the circuit it has a correcting effect and if enough capacity is used the power factor can be leading. F. C. Owens of Fayetteville, N. C., also helps power factor and the welding arc by use of capacitors in multiple, and wherever a capacitor is placed in or across all or part of an arcing circuit, it helps the arc and, of course, the power factor. Balancing of capacitance in the primary of the 40-40 circuit is also feasible but capacitors are only as reliable as storage batteries.

Another disadvantage of a-c welding, from an operating standpoint, is the fact that it operates single phase and very often not on a line built for single phase and the voltage droops and the welding withers accordingly.

With the multiple star system, as schematic wiring diagram and photographs show, there is an adequate tank or reservoir in the shape of the mother transformer absolutely to preclude any drooping. With the a-c multiple system the efficiency is three times better than with a multiple d-c system and twice as good as that of any single-motor-generator system. The independent control of both amperage and voltage at each station also removes one of the disadvantages of d-c multiple system where only one voltage is available for the entire system. D-c multiple installations would be used more if their efficiency were not so poor.

A typical welding-shop floor plan is shown in figure 8, with mother transformer *A* star connected, neutral grounded to building steel, rails, work tables, etc., and one cable to each general location to outlet transformers along the wall, overhead, or generally out of the way. The operator has but one welding lead. D-c motor generator sets have two welding leads, three power

Figure 8. Multiple star system



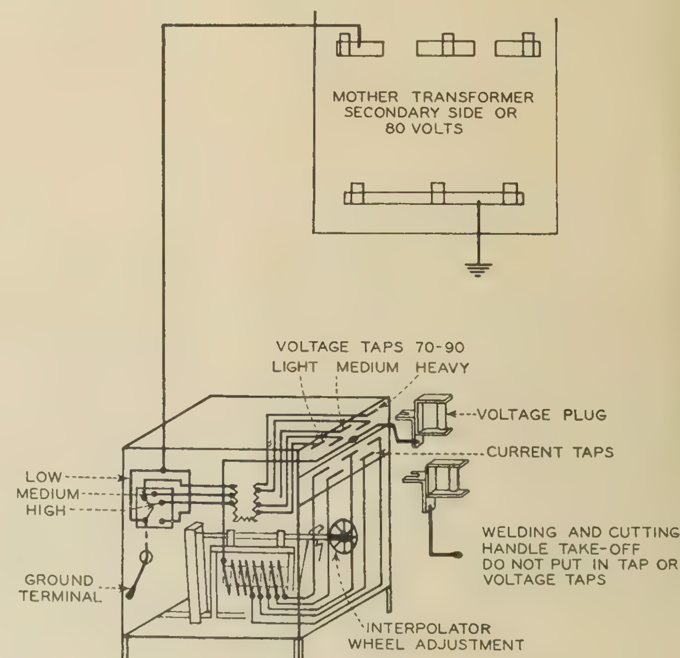


Figure 9. Detail, mother transformer and station

leads, and a motor generator for each arc.

Still another great advantage of the multiple star system is the space in the shop is not interfered with welding apparatus, nothing but outlets on the wall being necessary for this system.

The 220-, 440-, 550-volt power lead wires to the alleged portable single-operator machines are done away with and a very great hazard of fire and safety is obviated.

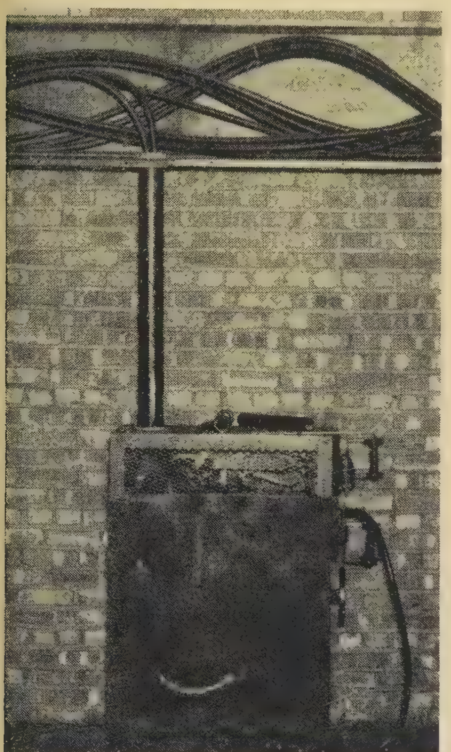
The second greatest detriment to single-operator transformer operation is interference which is a by-product of electrical line drop. The mother transformer reservoir system precludes this and I^2R is a negligible component where reactance control is used. In single-operator installations, part of each unit is tied up if the full capacity is not used.

D-c multiple systems are popular where one single voltage and no polarity reversing will do but the resistance method of control of d-c installations makes them very costly to operate and the large motor and its losses makes fractional operation very uneconomical. This is obviated in the multiple star system (figure 9). In single-operator installations there can be no diversity factor of the equipment. In the multiple arrangement the entire output can be had or any fraction of it with the maximum diversity factor and efficiency and no stand-by losses.

The a-c arc is one-fourth of the cost of a multiple d-c system and one-half that of any d-c motor generator set.

Where the work is heavy enough to stand the reservoir capacity, chipping and caulking are not necessary, and up to $1^1/_2$-inch-thick plates, this system has accomplished this desirable step forward with the testing and consent of the interested authorities, both the insurance, The American Society of Mechanical Engineers and the National Inspection Bureau. This is a saving of 50 per cent alone and alternating current is faster than direct current generally.

This multiple ring system (figure 10) is a planned installation. The country's welding progress has arrived at a point where this Topsy-like method of growing by adding single-operator machines should be superceded by an orderly designed welding power plant. In this connection, this system fits in beautifully with electric power, steam, or Diesel installations, or combinations of either. This multiple star system, which provides great chunks of power where needed without affecting the light and medium work outlets, is also ideal for the submerged-arc heavy welding or any requirement where combined amperage is necessary.



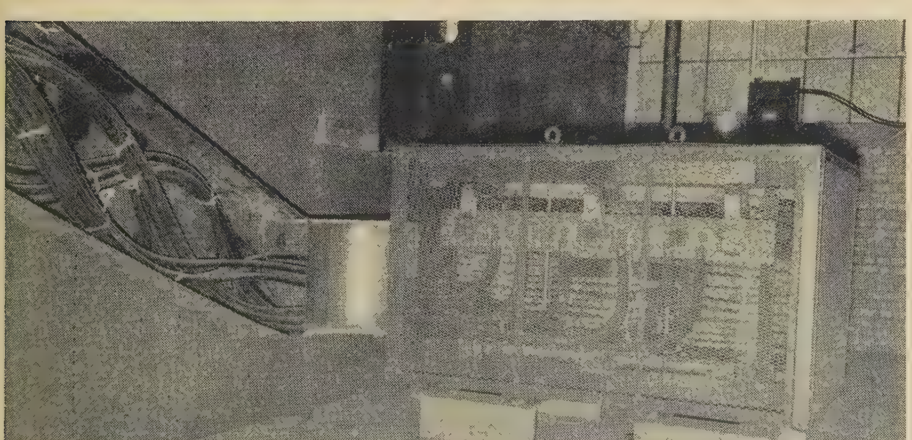


Figure 10. Multiple star system

Hamler boiler, Chicago

Figure 11. Public Service preheating welding

The latest development of induction hysteresis stress-relieving operates perfectly with this system. An illustration of two pipe joints preheated, welded, and normalized with the Smith-Dolan a-c preheating development is shown in figure 11. There were 8,900 such welds at the Essex plant of the Public Service Electric and Gas Company accomplished perfectly with their 60-cycle power. The test pressure on these joints was 2,250 pounds; the operating temperature was 950 degrees Fahrenheit. There were no leaks or failures of any kind. The metal was that tricky alloy to weld, carbon molybdenum steel.

The d-c arc is better for overhead and some vertical welding. The general use of covered electrodes has reduced this advantage of direct current materially and most industrial work is now "positioned."

Alternating current has become the steam shovel of welding, leaving direct current for the special work tentatively.

Discussion

A. U. Welch (General Electric Company, Pittsfield, Mass.): As pointed out in this paper, a-c arc welding has made great strides recently, largely due to the fine performance obtained from modern covered electrodes. The remarkable improvement

in arc stability obtained from these newer covered electrodes has almost eliminated the necessity for complicated welding circuits and equipment, and excellent results are being obtained with simple adjustable reactance control of arc current.

The circuit shown in figure 2 is merely a regulating autotransformer with its series winding connected bucking. It gives exactly the same performance as any other reactance-controlled transformer with the same open-circuit voltage and current. The circuits shown in figures 3, 6, and 7 use capacitance in series with inductive reactance to improve arc stability or to permit lowering open-circuit voltage somewhat with the same arc stability. The circuit operates in such manner that if an arc should fail to restrike after passing through normal current zero, the voltage across the reactor, or the equivalent reactance of the transformer, collapses immediately, leaving a charge on the capacitor. The voltage available to restrike the arc is then the sum of the capacitor and transformer open-circuit voltage, which will be higher than the normal open-circuit voltage. The capacitor soon discharges through its transformer, autotransformer, or discharge coil, and the circuit returns to normal open-circuit voltage. Since this is a series resonant circuit, extreme care must be taken in applying it to arc welding in order to avoid dangerous overvoltages on the capacitors, reactors, and transformers. Operation would not be so precarious if no adjustment of current output were required.

In order to obtain improved arc stability, it is necessary that the capacitive reactance partly compensate for the inductive reactance, or vice versa. In order to control current, either the inductive or capacitive reactance must be varied, and if the range of current is at all wide, the two reactances are likely to become so nearly equal as to form a completely resonant circuit with very high voltages on both capacitor and reactor. Furthermore, any additional reactance inserted in the circuit, which may be inherent reactance in the primary and secondary leads, will greatly disturb the operation of the circuit, change the current output to a very marked extent, and in some cases throw the circuit into resonance. Even the normal starting transient currents of the reactor and capacitor are apt to change the inductance sufficiently to cause resonance troubles.

A connection of capacitors in shunt across the primary leads of an arc welding transformer improves power factor, but has no effect on the welding arc except possibly to help hold the primary voltage up to its rated value.

The "multiple star system," which is merely a system of distributing power at low voltage instead of primary voltage, in general offers no advantage over distributing at usual primary voltages, such as 220, 440, or 550 volts. The price of adjustable reactors for the multiple star system is essentially the same as the price of arc welding transformers designed to run off primary voltage, and the elaborate system of heavy cables required for distributing at low voltage is much more expensive than the usual primary wiring in conduit. This is particularly true if isolating switches are used at each welding station, as is the usual practice, to prevent one faulty unit from shutting down the entire system. The cost of switches for the low-voltage high-current system is very much more than for the primary circuit switches.

The adjustable-reactance welding transformer which is the accepted standard equipment for a-c arc welding has the advantages over the circuits presented in this paper of simplicity, low cost, low maintenance, adaptability, and freedom from overvoltages and other dangerous phenomena.

R. F. Wyer (General Electric Company, Schenectady, N. Y.): As always, Mr. Holslag's paper stimulates a great deal of thought, as he doubtless intended it to do. One idea that seems to me needs further clarification is the use of a three-phase mother transformer and individual welding stations to counteract voltage droop in power circuits that lack stiffness. If a power line into a welding shop is too light to handle the additional load imposed by a-c welding, then the only means for enabling it to carry this load without additional voltage droop is the installation of capacitors, synchronous motors, or synchronous condensers to give a leading-power-factor load sufficient to bring the power factor of the whole plant load up to the point where the total kilovolt-ampere input will not exceed the kilovolt-ampere input previously existing in the plant. That is to say, sufficient leading reactive kilovolt-amperes must be furnished in the form of capacitors or synchronous machines to cancel the lagging reactive kilovolt-amperes of the welding load, and in addition some of the lagging reactive kilovolt-amperes of the other plant load. Only by this means could the total kilovolt-ampere load of the plant, and thus the voltage regulation of the power line be held constant in spite of the addition of the welding load.

The reference to the mother transformer as a tank or reservoir to preclude voltage droop is not clear to me. It might be thought that a three-phase transformer would distribute a single-phase load over all three phases of a three-phase power circuit. This, however, is not true, since a single-phase load on the secondary of a three-phase transformer will result in single-phase power input to the transformer, and the unbalancing effect on the power supply will be just the same as if only a single-phase transformer were used. It is true that if all three phases of the mother transformer are loaded on the secondary side, then the welding load will be distributed on the power line. However the same thing can be accomplished by simply distributing ordinary welding transformers on the three phases of the power circuit, and the load distribution will be accomplished.

It is difficult to see how there could be any economy in distributing welding power at low voltage, such as 80 volts, around a welding shop. This practice is quite the opposite of the trend in power distribution, where the tendency is to go to higher voltages in order to save copper.

Returning to the voltage droop consideration, it would seem that extraordinary precautions would have to be taken to avoid even increasing the voltage droop through the use of low-voltage power distribution to the various welding stations on account of the reactive drop in voltage which is inherent in all a-c distribution circuits. This is directly proportional to current, and since the current carried by the distribution circuit must necessarily be increased when using low-voltage distribution, I would expect that the trouble due to voltage droop would be accentuated rather than counteracted.

To use specific figures, suppose three welders are each using 250 amperes at various locations in a shop. With the ordinary single-operator transformer welder set-up, individual 440- or 220-volt leads, one pair from each phase, would be run to the individual transformers. Assuming 440-volt distribution, the current in these leads would be about 46 amperes. The actual primary current would depend on the open-circuit voltage on the secondary of the welding transformer, but 46 amperes is figured on the basis of an 80-volt open-circuit voltage, by multiplying the welding current by the ratio of transformation of 80/440.

With an 80-volt distribution system, and assuming that 80 volts open-circuit voltage is desired by the operator, then the current in the distribution circuit between the mother transformer and the individual welding stations will be 250 amperes, because there is no ratio of transformation in the individual welding station. Of course, if the open circuit voltage at the welding station were reduced to 60 volts, then the current in the 80-volt distribution circuit would be somewhat less; it would be $6/8 \times 250$, or about 190 amperes.

Remembering that the loss due to I^2R in the distribution lines is proportional to the square of the current in those lines, with a ratio of 46 to 200 amperes, the ratio of the weights of copper which will be required to give the same loss in the two systems will be 30 to 1.

In view of the foregoing considerations, it appears to me that a low-voltage distribution system such as is proposed by Mr. Holslag would result in an increase in interference between operators due to voltage drop in the supply lines to the individual welding stations, instead of a decrease as suggested in the paper.

K. L. Hansen (Harnischfeger Corporation, Milwaukee, Wis.): The paper is replete with assertions which no doubt will be accepted for what they are worth. For example, it is stated that the efficiency of the a-c multiple system is twice as high as that of any single-motor-generator system. As the efficiency of some motor generator sets is above 50 per cent, the efficiency of the a-c multiple system should be over 100 per cent, which is, to say the least, doubtful.

There is, however, one statement in the paper which will for a certainty be accepted without reservation, namely, that the author has preached the advantages of a-c arc welding for 26 years. In view of Mr. Holslag's statement that the acceptance of the a-c arc has now become universal, it would seem that the endless repetition in the paper of arguments which have become not only familiar, but threadbare through 26 years of preaching, should be superfluous.

Perhaps the acceptance of the a-c arc has not been so universal as this statement asserts. Indeed, what follows immediately

tacitly implies that such is not the case. The question is asked, "Why has a-c arc-welding progress been so retarded?" Mr. Holslag answers that the chief reason is the high open-circuit voltage, which lowers the power factor and is liable to cause nervous shocks, especially in wet places.

In the following two paragraphs Mr. Holslag gives a hint of what might have been an interesting topic for an engineering paper, namely, a method he has recently developed for overcoming the drawback to a-c welding just mentioned. The description is, however, entirely too inadequate for anyone to form an intelligent conception of its operation, let alone judge its merits. Had the paper omitted the great deal of needless repetition of old arguments and concentrated on an engineering discussion of this new development, it might have been a good one.

Mr. Holslag states that in Europe progress in a-c welding has preceded ours. C. H. Jennings, research engineer of the Westinghouse company, has recently returned from Europe where he spent considerable time studying various phases of welding. He has written at least one article and given a number of addresses on various phases of European welding practice. In Germany he found that the d-c arc preponderates, undoubtedly because of the great use of bare electrodes still prevalent there. The situation in England is interesting.

According to Mr. Jennings, the use of coated electrodes became prevalent in England at the very inception of the metallic arc-welding process. Furthermore, the strict requirements regarding inrush currents of induction motors made starting equipment expensive and militated against the use of motor generator sets. Hence, the conditions were ideal for a-c welding, and that is about all there was at the beginning. Some years ago, however, enough d-c welding had developed here to make the ratio about 20 per cent direct current to 80 per cent alternating current. The continued increasing use of direct current has made that ratio at the present time 40 per cent direct current to 60 per cent alternating current, and Mr. Jennings estimates that continuance of the present trend will shortly make it a 50-50 ratio. What will happen after that is problematical. If Great Britain possessed an ardent advocate of d-c welding, who had been preaching its advantages for a quarter of a century, he would now undoubtedly be prepared to publish a paper on "The D-C Arc Progresses."

Recently, when Mr. Jennings addressed the Milwaukee section of the American Welding Society on this subject, he was asked if he could account for this rapid increase of d-c welding in England. His answer was that the inability of the a-c arc to weld successfully nonferrous metals, such as aluminum, copper, copper alloys, nickel, nickel alloys, nickel clad steel, etc., and the superiority of the d-c arc in welding of some alloy steels and in vertical and overhead welding, are unquestionably factors in this changing ratio of d-c to a-c welding in England. Even Mr. Holslag concedes that d-c welding has these advantages, and for that reason gives it a lease of life, although only a tentative one.

There is one other statement in the paper that I in general agree with, although not 100 per cent. In his concluding paragraph Mr. Holslag says that alternating current has become the steam shovel of welding. It is well known, however, that the steam shovel has become an entirely antiquated piece of machinery, having completely given way to the internal combustion engine, that is, the gasoline or Diesel engine, and to some extent to the electric-driven shovel. Mr. Holslag did well in choosing an analogy, although I would not go so far as he does and compare a-c welding with the steam shovel, which is a completely outmoded piece of equipment. That is entirely unfair to a-c welding. There is, however, one strong similarity. If a steam shovel today were to be taken in trade for an up-to-date machine, its trade-in value would approach the vanishing point. It has been our experience that the same holds true when we are confronted with the situation of taking a welding transformer in trade for a motor generator set.

W. Richter (A. O. Smith Corporation, Milwaukee, Wis.): This discussion is confined to an analysis of the so-called "basic buck-back circuit" as shown in figure 2 of the paper.

For the purpose of studying the behavior of the circuit, replace the arc by a variable

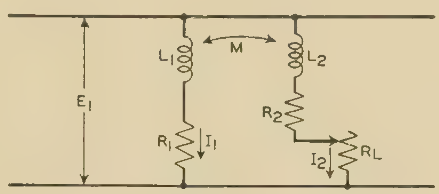


Figure 1

load resistance and the transformer by two coils, having self- and mutual inductance, as shown in figure 1 of this discussion.

Let

$z_1 \;=\; R_1 + jwL_1$ = primary impedance
$z_2 \;=\; R_2 + jwL_2$ = secondary impedance
$x \;=\; wM$ = mutual impedance
R_L = load resistance
E_1 = primary voltage
E_2 = secondary voltage
I_1 = primary current
I_2 = secondary or load current

We have then:

$$E_1 = I_1 \times z_1 + I_2 jx$$
$$E_1 = I_1 \times jx + I_2 \times (z_2 + R_L)$$

Eliminating I_1, results in

$$I_2 = E_1 \times \frac{z_1 - jx}{z_1(z_2 + R_L) + x^2}$$
$$= E_1 \times \frac{(z_1 - jx)/z_1}{z_2 + \dfrac{x^2}{z_1} + R_L}$$

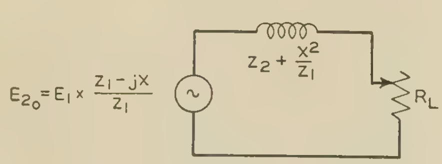


Figure 2

The secondary voltage is found by multiplying I_2 with R_L. This results in

$$E_2 = I_2 \times R_L = \frac{E_1 \times \dfrac{(z_1 - jx)}{Z_1}}{1 + \left(z_2 + \dfrac{x^2}{z_1}\right)/R_L}$$

For $R_L = \infty$ we obtain the open-circuit secondary voltage

$$E_{2_0} = E_1 \times \frac{z_1 - jx}{z_1}$$

Putting this into the equation for I_2, we obtain

$$I_2 = \frac{E_{2_0}}{z_2 + \dfrac{x^2}{z_1} + R_L}$$

This shows that the secondary current is the same as if we employed a generator or transformer with a terminal voltage E_{2_0} and a fixed impedance $z_2 + (x^2/z_1)$ in series with the load as shown in figure 2 of this discussion.

Thomas M. Hunter (American Transformer Company, Newark, N. J.): Mr Holslag's paper is of considerable interest to the welding industry because it describes a number of circuits for improvement in a-c welding. Up until the present time direct current has been largely used for arc welding and alternating current has been used to a lesser extent. Economically alternating current has many advantages over direct current. The reasons for this are as follows:

1. The load factor on the average welding machine is very low and the difference between the running-light losses of a generator and transformer is very high, consequently the operating efficiency of the transformer is very much greater than the generator.

2. The maintenance cost of transformers is negligible as compared with generators.

3. Transformers can be built so that they are much easier to install and transport from time to time than is the case of generators.

The reasons why a-c welding has not been more generally used are as follows:

1. The transformers, as built up to the present time, have open-circuit voltages from 80 to 105 and this has been considered dangerous by many users of welding equipment.

2. The power factor of these transformers is very low, ranging from 20 per cent to 50 per cent lagging.

3. These transformers draw a very large single-phase current and due to the low power factor this affects adversely the line voltage which causes disturbance on other apparatus.

4. In many of the transformer designs the means of controlling current has been awkward and very unsatisfactory from the operating standpoint.

5. Due to the very high reactance of transformers the length of secondary leads has affected the current output to such an extent it was impossible to obtain desired currents at great distances from the transformer.

In Mr. Holslag's paper he shows a number of circuits which tend to overcome these objections. Figure 2 will lower the open-circuit voltage and afford satisfactory welding operation but it does not improve the power factor. In figures 3, 6, and 7 he shows the use of capacitors which do aid in improving power factor as well as lower the open-circuit voltage; in figure 7 a circuit

is shown where reactance and capacity is used in series with the secondary circuit of the transformer. This gives very desirable characteristics for arc welding. The capacitors are not in the circuit at no load and are introduced into the circuit in such a manner that there is no tendency for instability, dangerous voltages, etc., and the apparatus is quite simple. Therefore, it is possible to obtain operating characteristics as shown in the data given hereinafter. We are giving data on both a reactance transformer and a resonant-circuit transformer.

A. U. Welch, in his discussion, explains the action of this circuit and I believe his explanation is about what takes place. The welding current builds up voltages across the capacitance and the reactance higher than the impressed voltage and the arc is stabilized due to the fact that the open-circuit voltage and reactance voltage come into play just at the correct moment to maintain the arc. This of course means that a lower voltage can be used at open circuit and still maintain arc stability at least equal to that obtained with reactance transformers. There have been a great many devices offered for lowering this open-circuit voltage and the ones used in the past have been of a mechanical nature such as contactors. This circuit gives a result which is entirely automatic and requires no moving parts.

The control is designed so that a range of current from 20 per cent to 100 per cent can be obtained with stability throughout. The circuits are so interlocked that it is impossible to get complete resonance which would build up excessive voltages. The control is very simple requiring only a relatively few seconds to change from maximum to minimum or vice versa. It is made either for installation in the transformer or external, in which case it can be installed at a distant point. In many installations it is desirable to have the welding transformer installed on the balcony or some other place so as to conserve floor space and the control unit, being relatively small, can be installed near the operator, the interconnecting wires being of low capacity requirements. In this respect this control duplicates the practical results of a generator control. It is a very simple matter to make the control device motor operated.

Table I

Test	Reactance Transformer	Capacitor Transformer
Drop in line voltage when thrown on line (per cent)	15	0
Power factor (per cent)	51 lagging	88 leading
Time required to obtain complete range in current	2 minutes	10 seconds
Change in current when secondary leads were increased from 60 foot length to 350 feet (per cent drop)	35	20
Current drawn on primary (amperes)	120	65
Open-circuit voltage on the secondary (volts)	83/105	64
Electrical efficiency (per cent)	88	87

Comparative tests of the capacitor control transformer versus the reactance control, 500-ampere capacity in both cases, give the data in table I of this discussion. In making these tests, a circuit of 150-kva capacity, 440 volts, 60 cycles, was used. All data given were taken at 500 amperes output from the secondary.

Summing up, I would like to refer to a statement made by Mr. Welch which I quote as follows:

> The adjustable-reactance welding transformer which is the accepted standard equipment for a-c arc welding has the advantages over the circuits presented in this paper of simplicity, low cost, low maintenance, adaptability, and freedom from overvoltages and other dangerous phenomena.

In my opinion no device is accepted as standard when better equipment is available. The comparative data given here fully justify the consideration of the resonant circuit for arc welding particularly when these advantages can be had with no sacrifice in operating characteristics.

Claude J. Holslag: The leading electric-welding companies in this country have fought the acceptance of the truth that the combination of a-c transformer welding and covered electrodes was and is the greatest advance in metal construction in its entire history. The large welding manufacturers made a concerted effort to stifle this advance to the art. The kindest thing I can say is they were innocent of the advantages to this country of this combination. The General Electric Company is apparently following the same policy in regard to the low-voltage developments and multiple star system. I notice Mr. Hansen is still of the d-c opinion that prevailed a decade ago. I would like to call his attention to the fact that even the Lincoln company has been attempting to sell a revolving a-c welder for the last five years and is now announcing a static transformer welder and, hence, friend Hansen is the last of the d-c "die hards."

The statement is made in the discussion of my paper that our multiple star system is not different from a grouping of single operator sets. In regard to this, I would like to refer to a three-page article in the April 1938 *Welding Engineer* which was written without my knowledge by Messrs. McGuire and Wood, president and general manager, respectively, of the Hamler Boiler and Tank Company, Chicago, where a 7,200-ampere installation was made by our company. We are listing a summary as made by another large company.

Multiple-Star-System Comparison Table With Other Arc-Welding Equipment

Versus A-C Single Arc Sets

1. Initial cost less
2. No voltage-drop interference
3.* Better load factor
4. Planned system; no overloaded circuits
5. Balance of phase load

Versus D-C Single Arc Sets

1. Initial cost less
2. Consumes less power—no idle loss—more efficient
3.* Advantage of load factor
4. Planned system with no overloaded circuits
5. Uses common ground

Versus D-C Multiple Arc System

1. Initial cost less
2. Consumes less power—no idle loss—more efficient
3. No interference of operators
4. Open circuit not fixed, but is arranged for adjustment

* Under better load factor, I would like to add the explanation that with single operator sets it is certainly obvious that if a fraction of their capacity is used for welding the rest of their capacity is not available but with the multiple star system half of the unused capacity of, say, two single operator sets could be added to create a third welding station. It is our actual experience that the capacity of this system is doubled because of this fact.

In regard to the reference that a man came back from Germany, who saw bare wire and d-c welding, this is due to one reason, namely; they dislike England and England's advance in welding with covered wire. However, for the last ten years, covered wires have been permeating Germany from all directions. Our Holland and our Scandinavian agents advise they have been having great success with alternating current and covered electrodes for the last ten years all over Europe, especially in Germany. Any person who could not see the advantages of covered wire now would be obtuse, indeed.

So as not to smother this triumph with words I will just point out that the exception noted in Mr. Hunter's discussion is the reactance means of controlling the current, which this company has developed, by which any amount of current can be varied from 10 per cent to 100 per cent with no greater power required under load than open circuit. Comparing this to, say, reactance controls which require two arms of a strong man to open and a very heavy motor for remote control and with which scheme it is practically impossible to move under load, I am quoting Doctor Comfort A. Adams that he cannot understand why the electrical industry has missed such a simple electromagnetic mechanical current-varying solution as we have developed.

Although Mr. Richter presents a very simple and interesting analysis of my basic buck-back transformer, the method which he employs is wholly unsatisfactory for power transformers and was abandoned many years ago as far as that application was concerned. The reason for this abandonment was that this method involved a determination of a relatively small quantity by the difference between two relatively large quantities, both of which were hypothetical and widely variable.

In other words, L_1, L_2, and M as used in Mr. Richter's discussion are all widely variable.

If Mr. Richter's conclusion is correct, to the effect that this arrangement works the same as would a simple leaky welding transformer with an open-circuit voltage equal to E_{20}, it is obvious that this arrangement has no advantage. That this conclusion is not correct is obvious from the following fact: A welding transformer with an open-circuit voltage of 45 or 50 will not maintain a stable arc, whereas my buck-back transformer with an open-circuit voltage of 45 or 50 does maintain a stable arc.

The explanation of this fact is not obvious, but may be due to some short-time transient effect, the analysis of which is too difficult for me to tackle.

If the problem were as simple as Mr. Richter seems to think, it would have been solved long ago.

Transactions

Papers and Discussions Comprising Pages 371–434 of the 1939 Volume

Effect of Restriking on Recovery Voltage

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IT IS well known that when any portion of an electrical circuit is either opened or closed a transient condition will usually exist for some period, depending on the damping, before the circuit assumes its new steady state. In power circuits, such switching operations are the application and removal of faults or loads and the connecting or separating of various parts of a system. The transient conditions resulting from these switching operations give rise in some cases to overvoltages, which can be calculated by straightforward and more or less well-known methods, if the transient circuit parameters are known. However, in some instances voltages much higher than those predicted by such calculations have been obtained, and various explanations[1–9] have been offered for these occurrences.

In the present paper, it is suggested that some of these cases of excess voltage arise from the opening of a circuit which is already in a transient condition, the transient condition being caused by immediately preceding switching operations. For example, under certain circuit conditions they may arise from successive momentary clearings and re-establishments of a circuit at such short intervals as to allow comparatively little decay of the transients between these events. Under such conditions it is shown to be theoretically possible to build up extremely high voltages, although tests have indicated that the actual voltages realized are always less than the theoretical maximum.

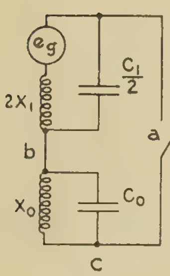

Figure 1

General Analysis

This phenomenon may occur with circuits which may in the simplest case be reduced to that of figure 1. Here capacitance may exist also across the switch, but the only capacitance essential to the process is C_0, so all other capacitances will be neglected at this point. Generation may be either at the point shown or in the x_0 branch, without influencing the analysis except for the voltage *b-c* (that is, across x_0).

Let $2x_1$ be much less than x_0.

Now suppose that there is an arc at *a* which completes the circuit at this point. At each current zero there will be an attempt at extinction, and voltage will rise across the arc path either to complete the recovery transient and interrupt the circuit, or to cause a new breakdown of the arc path and re-establishment of the arc. If breakdown occurs, three components of current will flow; a resumption of the normal-frequency current, a d-c component, and an oscillation involving C_0, $2x_1$, and x_0. The frequency of this oscillation will usually be considerably greater than normal frequency. If it is very high the d-c component becomes very small and in the limit may be neglected without affecting the nature of the process to be described. The two remaining components initially have the same polarity, but in the second half-cycle of the natural-frequency component the polarities are opposite. Then if the restrike occurs at a sufficiently high point on the recovery transient and if $2x_1$ is considerably smaller than x_0, the instantaneous value of current in the natural-frequency component will exceed that of the normal-frequency component and the net current will tend to pass through zero. This gives an opportunity for a new attempt at interruption, which may result in a repetition of the process.

The manner in which this process may build up voltage is as follows, if generation

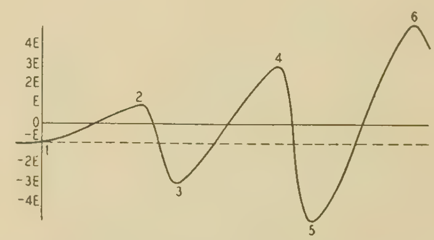

Figure 2. Build-up of voltage at point b of figure 1 due to successive clearings and restrikes within a normal half cycle

Odd-numbered points are clearings, even-numbered points restrikes. After clearing, voltage tends to oscillate about 0, after restrike it tends to oscillate about $-E$

is as shown in figure 1 and interruption occurs at the peak of a voltage wave.

It is evident from inspection that the steady-state voltage *b-c* is zero when the circuit is open at *a*, and substantially equal and opposite to the generated voltage when current is flowing at *a*. Therefore, if E is the generated voltage, which may be taken as equal to normal peak voltage, at the time of interruption the voltage *b-c* tends to change from $-E$ to 0; in doing so it will oscillate to $+E$. The first half cycle of this oscillation is given by the section between 1 and 2 of the curve of figure 2.

At this time the voltage at *a* is $2E$, and the arc may restrike. If it does, the voltage *b-c* tends to change from $+E$, to $-E$, and will therefore oscillate to $-3E$, as indicated by the section from 2 to 3 of the curve of figure 2.

At about the time when $-3E$ is reached, the net current may pass through zero and a second interruption may take place. The voltage *b-c* then starts to change from $-3E$ to zero, and may therefore oscillate to $+3E$, as from 3 to 4 of figure 2.

Paper number 39-50, recommended by the AIEE committees on power transmission and distribution, and protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 26, 1938; made available for preprinting December 20, 1938.

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1. For all numbered references, see list at end of paper.

The arc may then restrike again, and thus the process may repeat with a possible voltage increase of considerable magnitude at each repetition.

The voltage at a will, of course, go through changes somewhat similar to those of the voltage b-c.

This analysis has idealized the situation somewhat both as regards system performance by neglecting such items as the change in generated voltage, some of the effects of the presence of x_1, and decrement of the oscillations, and as regards performance of the arc by assuming restrike always to occur at the peak of the recovery transient.

A more exact treatment is given in appendix I, and from this are plotted the curves of figure 3, which apply to the voltage across the switch at a rather than to the voltage b-c. Even here, however, it has been necessary to make some assumption with reference to the time of restrike, and naturally the worst reasonable condition was assumed. As a result, the rate of build-up of overvoltages in most actual cases is less than that indicated, even to the extent that there may not be progressive build-up. Instead, successive voltages may be of a more or less random nature. Figure 4 shows an

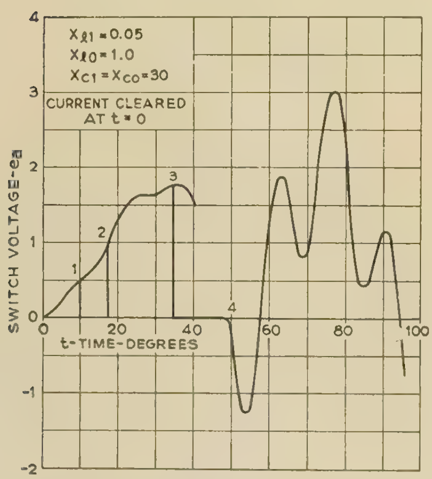

Figure 3. Switch voltage on interruption of circuits of figures 1 or 8

1—Restrike at e_{arc} = 0.5

2—Restrike at e_{arc} = 1.0

3—Restrike at e_{arc} = 1.77

4—Second clearing

oscillogram illustrating this phenomenon.

The circuit of figure 1, both with the capacitance $C_1/2$ connected as shown and with it connected directly across the breaker, may appear in the single-line diagram of various circuits quite common in operation. Any fault supplied from a rather extensive system through a small and more or less concentrated single reactance may act in this manner. This reactance may be a transformer or reactor, or even a short length of transmission line or cable. A few cases are on record in which overvoltages have been experienced which are believed to be attributable to this phenomenon but they do not appear to be nearly so widespread as the commonness of the circuit might lead one to expect.

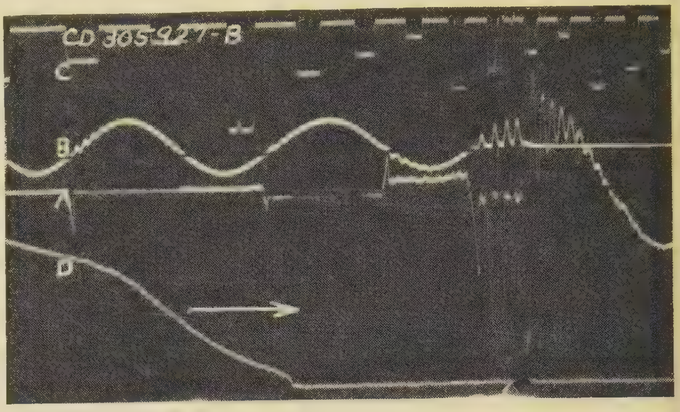

Figure 4. Interruption of circuit of figure 1

Curve *A*—Voltage

Curve *B*—Current

Curve *C*—Breaker travel

Curve *D*—Trip-coil current

Certain other cases have individual features which are discussed hereinafter under the following headings:

1. Ground faults on systems grounded through neutral reactance.
2. Interruption of line charging current.
3. Arcing ground faults on systems with neutral isolated or grounded through reactance.

1. Ground Faults on Systems Grounded Through Neutral Reactance

It is obvious from inspection that the interruption of a single-line-to-ground fault on a system grounded through a neutral reactor fits the circuit of figure 1. Here x_0 represents the zero-phase-sequence reactance, which will lie for the most part in the neutral reactor; C_0 represents the zero-phase-sequence capacitance, or one-third of the capacitance of the entire system to ground. x_1 and C_1 represent positive-phase-sequence reactance and capacitance, respectively. Generation is as shown. In case of fault interruption there is a tendency for the restrike voltage to increase at successive current zeros because of the continually increasing separation of the contacts of the interrupting device. When this restrike voltage becomes sufficiently great, the resulting current oscillation may become so large as to pass through zero in its first cycle. The process of successive build-up of voltage may then take place as explained in the general analysis and illustrated in figure 2 for the neutral voltage and figure 3 for the voltage at the interrupting device.

The methods of appendix I have been used to investigate the conditions under which this type of build-up may occur and it has been found that they may be expressed in terms of the reactance x_0 of figure 1. A lower limit is fixed by the fact that extra current zeros do not occur when x_0 lies below the values indicated by the curves of figure 5, although it should be pointed out that even if x_0 is below these limiting values a voltage approaching three times normal peak may appear at the neutral, as indicated at point 3 of figure 2.

Two points are of interest on these curves. When C_1/C_0 is equal to or greater than unity, corresponding approximately to the usual cable or overhead transmission system, a conservative value of x_0 is ten times x_1; but when C_1/C_0 is equal to zero, which may be approached in the case of a number of generators which are substantially independent electrically except for a common neutral bus, x_0 should not be greater than four or five times x_1.

Once this lower limit is well passed, no great change takes place as a result of increase of x_0 until it approaches equality with the capacitive reactance C_0, that is, until the condition of the ground-fault neutralizer or Petersen coil is approached. The ground-fault current in a system grounded through a ground-fault neutralizer is largely in phase with the voltage, so that the recovery voltage is very small and consequently restriking is unlikely. Even with poor tuning, a cycle or more is required for voltage to recover to normal, so that there is little chance of a restrike at a voltage high enough to cause serious build-up by successive clearings and restrikes.

Beyond the ground-fault-neutralizer value, the natural frequency becomes lower than the operating frequency and the phenomenon becomes substantially that of the interruption of transmission-line charging current.

If it is desired to use a neutral impedance within the danger zone, successive restriking may still be prevented by the use of a parallel neutral resistor of proper value or by the use of a neutral resistor instead of a neutral reactor. The parallel neutral resistance must be low enough to prevent, by its damping action, the restrike current oscillation from passing through zero. Of course, in addition to this main accomplishment, it also reduces the initial recovery voltage, particularly for single-line-to-ground faults.

Calculations have shown that the required parallel resistance is always somewhat greater than that required for critical damping of the ground or zero-sequence circuit, the difference depending on the circuit natural frequencies and on the ratio of zero-sequence to positive-sequence reactance. It is therefore suggested that as a practical and conservative means of selecting the proper resistance, this critical damping value R_c be used. The value of this resistance is given approximately by

$$R_c = \frac{1}{2}\sqrt{x_{ln}x_{cn}}$$

where

R_c = shunt resistance required for critical damping

x_{ln} = inductive reactance of the ground circuit
= one-third of the zero-sequence inductive reactance for a three-phase system

x_{cn} = capacitive reactance of the ground circuit
= one-third of the zero-sequence capacitive reactance for a three-phase system

It may be seen from this equation that the power rating of the required resistor for a given neutral reactor is approximately proportional to the square root of the system capacity to ground.

The discussion above applies directly to single-line-to-ground faults on reactance-grounded systems. However, even in case of two- or three-phase faults to ground there is always a last phase to clear, and this may behave like a single-line-to-ground fault. Thus, the discussion applies qualitatively to all faults involving ground.

It is also evident that the neutral reactance under discussion may be that of a grounding transformer, rather than a reactor, without essentially changing the circuit conditions although in this case there may not be any conductor at neutral potential, so that the neutral may be eliminated as a possible breakdown point.

2. Interruption of Line Charging Current

Upon interruption of the charging current of a transmission line, cable, or

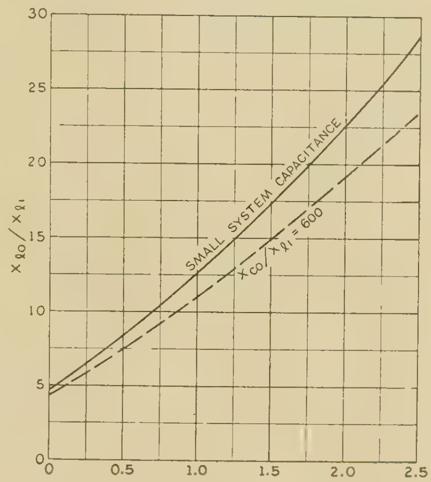

Figure 5. Critical ratio of zero-sequence reactance x_{l0} to positive-sequence reactance x_{l1}, as a function of ratio of positive-sequence capacitance C_1 to zero-sequence capacitance C_0—successive restriking may occur if x_{l0}/x_{l1} exceeds the critical value

capacitor at a current zero the rate of rise of recovery voltage is very slow. If, because of this fact, the circuit breaker interrupts at small contact separation and in addition has a relatively slow build-up of dielectric strength, the arc may restrike. When such restriking takes place, a current oscillation of large magnitude compared to the steady-state charging current occurs. This current may again be interrupted on its first zero, with a resulting recovery voltage which may be higher than normal. Continued restriking at successively higher voltages may take place in this case, just as in the case of the previous section, as the contact separation increases, until the final interruption is attained.

Such operation has been discussed in detail in references 8 and 9. An additional illustration is given in appendix II and figure 6, in order to show that the build-up of voltage may occur even with interruption of lumped capacitance current.

To prevent successive restriking it is desirable to build up the breaker dielectric strength as rapidly as possible after the first interruption and thus successfully prevent the first restrike.

Another method of attack is to introduce damping into the circuit by resistance in or across the breaker or line.

3. Arcing Ground Faults on Systems With Neutral Isolated or Grounded Through Reactance

A single-line-to-ground fault on an isolated-neutral system causes only a very small capacitance current to flow. Usually this current flows through an arc, which is interrupted at every current zero and requires a certain restrike voltage for reignition. The results of such action have been described for a particular case in references 1 and 7. A more or less constant restrike voltage may be expected, which limits the voltage on the faulted phase. The transient applied to the system at every such restrike and every subsequent clearing may lead to voltages in the unfaulted phases which in the steady state exceed the maximum transient voltages associated with simple application or interruption of a solid fault.

Curve A of figure 7 shows the maximum sustained voltages at the point of fault on the unfaulted phases of a three-phase system having a steady-state arcing ground on one phase. These voltages were obtained by tests on a miniature system.

For positive value of x_0/x_1 the curve of maximum obtainable voltage is affected considerably by the amount of system capacitance, the curve shown being the envelope of the many possible curves obtainable with particular values of capacitance.

It was found in test that if the restrike

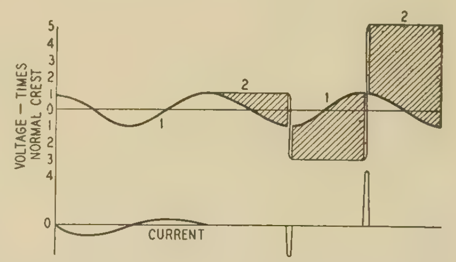

Figure 6. Interruption of capacitance current-circuit of figure 9

Curve 1—Voltage at inductance side of switch

Curve 2—Voltage at capacitance side of switch

Height of cross-hatched area gives voltage across switch

voltage was continually changing so that the phenomena were not periodic, the voltages on the unfaulted phase could be considerably increased. Such changes might be caused, in an arc to ground on a

power system, by wind or by changes in insulation characteristics.

For ready comparison, figure 7 shows also the steady-state fundamental voltage on the unfaulted phases during a solid

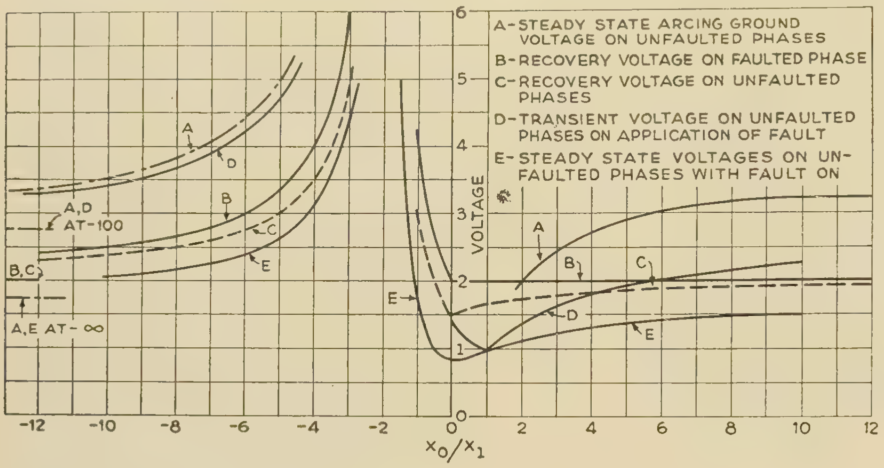

Figure 7. Single-line-to-ground fault on three-phase system—maximum voltages to ground at fault point in per unit of crest leg voltage before fault

jx_0 = zero-sequence impedance

jx_1 = positive-sequence impedance

line-to-ground fault (curve E) and the maximum transient voltage caused by the application (curve D) or removal (curves C and B) of such a fault. Resistance and arc drop are neglected in all curves except that for a sustained arcing ground (curve A). In general, fault resistance decreases the voltages on the unfaulted phases.

Conclusions

From the discussion given above and from the calculations and tests made, it may be concluded that:

1. In the course of the interrupting process on certain types of circuit, the current immediately following restriking of the arc after an early attempt to clear may involve high-frequency components of sufficient magnitude to cause the current to pass through zero very shortly after the restrike. Clearing at this time gives rise to voltages considerably in excess of those normally associated with the recovery transient.

2. Systems grounded through neutral reactors may be subject to these overvoltages.

3. If the possibility of these overvoltages is to be avoided, neutral-grounding reactors must satisfy one of the following conditions:

(A). Their reactance must be so low as to keep the zero-sequence reactance below

(a). Ten times positive-sequence reactance if the zero-sequence capacitance is no larger than the positive-sequence capacitance, or

(b). Five times positive-sequence reactance if the zero-sequence capacitance is much larger than the positive-sequence capacitance.

(B). Their reactance must tune the line capacitance to ground (ground fault neutralizer).

(C). They must be shunted by resistance to provide substantially critical damping.

4. The interruption of line charging current may give rise to high voltages unless restriking is prevented either by quick clearing by means of rapid build-up of dielectric strength in the circuit breaker, or by circuit damping.

Appendix I

In this appendix there is presented an approximate analysis of the behavior of a three-phase power system with neutral reactance grounding, during the clearing of a single-line-to-ground fault and with restriking of the arc after the first clearing. The system may be approximately represented by the circuit of figure 8, which in turn may be represented for the purposes of analyzing a single-line-to-ground fault by the circuit of figure 1.

It is assumed that:

1. Arc drop is zero.
2. All resistances are zero.
3. Arc interruption occurs only at current zeros determined by the circuit constants.
4. The arc restrikes at various values of switch voltage.
5. Positive- and negative-sequence impedances are equal.

Under these conditions the fault current before interruption is given by the equation

$$i_{a1} = \frac{\sin t}{Z_{0s} + 2Z_{1s}} = I_a \sin t \quad (1)$$

where

x_{c1} = positive-sequence capacitive reactance

x_{l1} = positive-sequence inductive reactance

Z_{0s} = steady-state zero-sequence impedance

$$Z_{0s} = \frac{x_{c0}x_{l0}}{x_{c0} - x_{l0}}$$

The normal voltage at the fault point is 1.0

Z_{1s} = steady-state positive-sequence impedance

$$Z_{1s} = \frac{x_{c1}x_{l1}}{x_{c1} - x_{l1}}$$

t = time measured from the instant the current is interrupted

x_{c0} = zero-sequence capacitive reactance

x_{l0} = zero-sequence inductive reactance

After the current is interrupted at $t = 0$, the recovery voltage on the faulted phase is

$$e_{a1} = I_a Z_{0s} (\cos t - \cos \omega_0 t) + 2I_a Z_{1s} (\cos t - \cos \omega_1 t) \quad (2)$$

or

$$e_{a1} = \cos t - I_a Z_{0s} \cos \omega_0 t - 2I_a Z_{1s} \cos \omega_1 t$$

where

I_a is defined by equation 1

ω_0 = zero-sequence natural frequency

$\omega_0 = \sqrt{x_{c0}/x_{l0}}$

ω_1 = positive-sequence natural frequency

$\omega_1 = \sqrt{x_{c1}/x_{l1}}$

The zero-sequence voltage is

$$e_{01} = I_a Z_{0s} \cos \omega_0 t \quad (3)$$

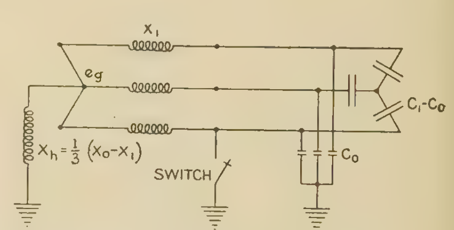

Figure 8. Typical three-phase system with single-line-to-ground fault

If restrike occurs at time $t = t_1$, the restrike current is

$$i_{a2} = -\frac{\sin t_1}{x_{l0} + 2x_{l1}} + \frac{I_a}{x_{l0} + 2x_{l1}} \times \left(\frac{Z_{0s}}{\omega_0} \sin \omega_0 t_1 + \frac{2Z_{1s}}{\omega_1} \sin \omega_1 t_1\right) + I_a \sin (t + t_1) + \frac{(\omega'^2 - \omega_0^2)(\omega_1^2 - \omega'^2)}{(x_{c0} + 2x_{c1})(\omega'^2 - 1)\omega'^2} \times [\sin t_1 \cos \omega' t + \omega' \cos t_1 \sin \omega' t] + \frac{I_a}{(x_{c0} + 2x_{c1})\omega'^2} [2Z_{1s}(\omega'^2 - \omega_0^2)(\omega_1 \sin \omega_1 t_1 \times \cos \omega' t + \omega' \cos \omega_1 t_1 \sin \omega' t) - Z_{0s}(\omega_1^2 - \omega'^2)(\omega_0 \sin \omega_0 t_1 \cos \omega' t + \omega' \cos \omega_0 t_1 \sin \omega' t)] \quad (4)$$

or

$$i_{a2} = I_0 + I_a \sin (t + t_1) + I_n \cos (\omega' t - \theta_n)$$

where

t is now measured from the point at which restrike occurred

t (equation 4) $= t$ (equation 2) $- t_1$

ω' = natural frequency with fault on

$$\omega' = \sqrt{\frac{(x_{l0} + 2x_{l1})x_{c0}x_{c1}}{(x_{c0} + 2x_{c1})x_{l0}x_{l1}}}$$

I_0, I_n, and θ_n are defined by equation 4

While this restrike current is flowing, the zero-sequence voltage is

$$e_{02} = Z_{0s}I_a \cos(t + t_1) - \frac{x_{c0}(\omega_1{}^2 - \omega'^2)}{(x_{c0} + 2x_{c1})(\omega'^2 - 1)\omega'} [-\sin t_1 \sin \omega' t + \omega' \cos t_1 \cos \omega' t] + \frac{x_{c0}I_a}{(x_{c0} + 2x_{c1})\omega'} \times [-2Z_{1s}(-\omega_1 \sin \omega_1 t_1 \sin \omega' t + \omega' \cos \omega_1 t_1 \cos \omega' t) + \frac{Z_{0s}(\omega_1{}^2 - \omega'^2)}{(\omega'^2 - \omega_0{}^2)} \times (-\omega_0 \sin \omega_0 t_1 \sin \omega' t + \omega' \cos \omega_0 t_1 \cos \omega' t)] \quad (5)$$

or

$$e_{02} = Z_{0s}I_a \cos(t + t_1) + \frac{x_{c0}\omega' I_n}{\omega'^2 - \omega_0{}^2} \sin(\omega' t - \theta_n)$$

Note that $\left(\frac{jx_{c0}\omega'}{\omega_0{}^2 - \omega'^2}\right)$ is simply the impedance of the zero sequence circuit at a frequency ω'.

If the current given by equation 4 passes through zero at $t = t_2$, where t is measured from time t_1, the circuit may then be recleared. After this second clearing the recovery voltage on the faulted phase is

$$e_{a3} = \cos(t + t_1 + t_2) + \omega_0 I_0 x_{l0} \sin \omega_0 t + \omega_1 2I_0 x_{l1} \sin \omega_1 t - I_a Z_{0s}[\cos(t_1 + t_2) \times \cos \omega_0 t + \omega_0 \sin(t_1 + t_2) \sin \omega_0 t] - 2I_a Z_{1s}[\cos(t_1 + t_2) \cos \omega_1 t - \omega_1 \sin(t_1 + t_2) \sin \omega_1 t] - \frac{I_n x_{c0}}{\omega'^2 - \omega_0{}^2} \times [\omega_0 \cos(\omega' t_2 - \theta_n) \sin \omega_0 t + \omega' \sin(\omega' t_2 - \theta_n) \cos \omega_0 t] + \frac{2I_n x_{c1}}{\omega_1{}^2 - \omega'^2} [\omega_1 \cos(\omega' t_2 - \theta_n) \sin \omega_1 t + \omega' \sin(\omega' t_2 - \theta_n) \cos \omega_1 t] \quad (6)$$

where

t (equation 6) $= t$ (equation 4) $- t_2$
$= t$ (equation 2) $- t_1 - t_2$

$$\frac{x_{c0}}{\omega'^2 - \omega_0} = \frac{2x_{c1}}{\omega_1{}^2 - \omega'^2} = \frac{x_{c0} + 2x_{c1}}{\omega_1{}^2 - \omega_0{}^2}$$

The zero-sequence voltage is the negative of the sum of all the terms of equation 6 having a frequency ω_0.

As an example of the application of these equations, a circuit with $x_{l0}/x_{l1} = 20$, $x_{c0}/x_{l0} = 30$, and $x_{c1} = x_{c0}$ has been considered. Figure 3 shows the recovery voltage on the faulted phase. If restrike occurs at half or normal voltage (points 1 and 2), the resulting oscillations of the restrike current do not pass through zero and the current continues for another half cycle; if restrike occurs at maximum recovery voltage (point 3), the restrike current oscillates to zero at point 4 and is there interrupted. The recovery voltage then rises to about three times normal, if no further restriking occurs. At the same time the zero-sequence voltage (which is nearly equal to the neutral voltage to ground) rises to about two times leg voltage.

By means of these equations and tests on miniature circuits with $x_{c1} = x_{c0}$, it has been determined that if $x_{l0}/x_{l1} < 10$, the restrike current cannot oscillate to zero regardless of the point of restrike. Therefore a second (or more) clearing is rendered impossible.

If $x_{c1} > x_{c0}$ the critical value of x_{l0}/x_{l1} is decreased, while if $x_{c1} < x_{c0}$, the critical x_{l0}/x_{l1} is increased, as shown by figure 5.

Appendix II

As an extremely simplified representation of the interruption of charging current one may consider the circuit of figure 9. Here the line or other capacitance is represented

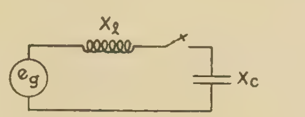

Figure 9

by a lumped capacitance x_c supplied through the source inductance x_l. If this circuit is interrupted at a current zero the recovery voltage in per unit of normal voltage at the capacitor is

$$e_{a_1} = \frac{x_c - x_l}{x_c} \cos t - 1 \quad (7)$$

If restriking takes place when the recovery voltage is maximum at $t = \pi$, the restrike current is

$$i_{a2} = \frac{\sin t - 2\omega \sin \omega t}{x_c} \quad (8)$$

where t is now measured from the instant of restrike, and

$$\omega = \sqrt{x_c/x_l}$$

It is evident that in general the natural-frequency component of i_{a2} is much greater than the fundamental component. Thus a large current oscillation of high frequency takes place and may be interrupted at its first current zero. If this occurs, the restrike current and subsequent recovery voltage, neglecting minor oscillations, will appear as in figure 6. By continuing the process begun by equations 7 and 8 we arrive at the successively higher voltages of figure 6.

It is not to be inferred that voltages of exactly this character exist. Instead, there is here also a randomness in the restrike voltage at successive intervals which largely controls the individual voltage peaks. In addition, the capacitance may be distributed along a transmission line as in references 8 and 9, and the voltage may also be affected by coupling between phases of a polyphase system. However, the tendency and the possibility of a building up of high voltages is clearly shown, so that it seems highly desirable to prevent such restriking.

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Discussion

R. A. Hentz (Philadelphia Electric Company, Philadelphia, Pa.): From the time impedances in the neutrals of generators were deemed desirable, resistances were used almost exclusively, if not entirely so, and to the best of my knowledge with complete satisfaction as far as electrical characteristics are concerned. Later grounding reactors instead of resistors were used in a number of cases, as they offered the advantages of smaller space, less cost, and of being less subject to deterioration. With the advent of transmission substations where the busses were supplied from delta-connected transformer banks, neutral grounding was obtained in many cases by means of zigzag grounding transformers.

The analysis made by the authors brings out (conclusion number 2) that systems grounded through neutral reactors may be subject to overvoltages considerably in excess of those normally associated with the recovery transient. This analysis is of considerable importance to those systems which use neutral reactors or where grounding transformers are employed which resulted in a somewhat similar electrical condition.

The importance of the subject is further emphasized by the fact that not only can difficulties result from neutral grounding through reactance, but that breakdowns have occurred both to machines and cables on systems for which this form of grounding gives a probable explanation.

Reactance grounding can be eliminated relatively easily where "wye"-connected machines or other equipment are employed by installing neutral resistors in place of the

reactances. This in my opinion is to be preferred to paralleling the existing reactors with a resistor. The situation is not so simple where the grounding transformers are employed. Such situations, therefore, present a challenge to designing engineers for the development of some equipment which will either replace or supplement these grounding transformers to the end that these dangerous overvoltages may be reduced to safe limits.

D. C. Prince (General Electric Company, Philadelphia, Pa.): This paper represents a valuable addition to the literature on voltage recovery transients. It will be noted that it contains actual test data substantiating at least the order of magnitude of the overvoltage due to an arcing ground predicted by Messrs. R. D. Evans, A. C. Monteith, and R. L. Witzke (AIEE TRANSACTIONS, volume 58, 1939, pages 386–97). The phenomena, however, consisted of more restrikes under less severe conditions than those postulated by Messrs. Evans, Monteith, and Witzke, so it is not certain that the apparent confirmation is more than an accident.

J. A. Adams (Philadelphia Electric Company, Philadelphia, Pa.): It is of interest to compare the limits of zero-sequence reactance given in conclusion 3 with values which have existed during faults on actual systems where the phenomena discussed have probably been the cause of secondary failures.

In two frequency-converter substations on the Philadelphia Electric Company system the neutrals of the 25-cycle generators were grounded through four-ohm reactors. While operating one of these substations with two generators connected to the 13.2-kv bus, one generator with the neutral grounded through the reactor and the other with the neutral ungrounded, a single-phase-to-ground fault developed on one of the cables fed from the bus. After the fault was cleared it was found that the neutral lead on the generator with its neutral ungrounded had broken down to ground. In this case the ratio of x_0/x_1 was 25.7, which is well above either of the lower limits given. For another fault on this system with only one generator operating the ratio of x_0/x_1 was 12.9 and a second breakdown did not occur. The ratio of C_1/C_0 for these two cases is estimated to be approximately unity.

At the other substation practically simultaneous faults developed to ground on one phase of the leads from one generator operating with its neutral grounded through the reactor and in the winding in the same phase of the second generator operating with its neutral ungrounded. In this case the ratio x_0/x_1 was 14.6 and the ratio of C_1/C_0 was approximately unity. At another time, however, a cable failure to ground with the same system set up did not cause a second breakdown, probably indicating that this ratio of x_0/x_1 is near the critical value.

As a result of an analysis of these cases of trouble, the neutral-grounding reactors are being replaced with four-ohm resistors.

R. D. Evans, A. C. Monteith, and **R. L. Witzke** (all of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Considering the paper by Messrs. Concordia and Skeats, we feel that it is essential to consider the arc voltage and the increase in voltage due to extinction of the arc prior to a normal current zero as this effect will give rise to higher voltages than indicated in their paper. This is of particular interest when considering grounding means for generators as the extinction voltage may be comparable to the normal line-to-neutral voltage and proportionately increase the transient voltages.

(See also discussion, page 412.)

C. Concordia and **W. F. Skeats:** The authors are particularly grateful to those discussers who have contributed their experience with overvoltages apparently coming within the scope of this paper. Circuits falling within the general classification susceptible to this type of overvoltage appear to be more widespread than is indicated by any trouble experience, and a study of trouble experience seems to afford the most practical means of setting down more precisely the conditions under which overvoltages may be expected. In this connection it should be noted that fundamentally the phenomenon is not limited to systems using neutral-grounding reactors but may occur on any system where, looking back from the point of fault, one sees first a small reactance, then an appreciable capacitance, and finally a comparatively large reactance.

High arc voltage preceding current zero tends to increase the voltage reached on the normal recovery transient, which indirectly increases the likelihood, upon restrike, of a high-frequency current oscillation of sufficient amplitude to cause the total current to pass through zero early in the cycle and so start the process of building up voltage. High arc voltage after a restrike also helps directly to bring about such an early current zero. However, once the build-up process has been started, the effect of arc voltage may be to retard the rate of build-up and limit the final voltage obtained rather than the reverse. Thus the authors cannot agree with the contention of Messrs. Evans, Monteith, and Witzke that arc voltage of reasonable value will greatly increase the voltages reached by the build-up process discussed in the paper, although of course, extremely high arc voltages may result in dangerously high recovery voltages even without this build-up process. In presenting their paper the authors felt that the overvoltages arising from successive restriking with certain critical circuit constants should be separated from those arising from high arc voltage.

The elimination of trouble in the case of zigzag grounding transformers may be accomplished either by using a grounding transformer whose reactance satisfies the conditions of conclusion 3*A* or 3*B*, or by using sufficient series resistance between the transformer neutral and ground, although this last remedy might in some cases reduce the ground currents too much for proper relaying.

Overvoltages During Power-System Faults

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OVERVOLTAGES may be produced by lightning, switching surges, faults, both solid and arcing, and the overspeeding of machines due to loss of load. The effect of circuit and machine characteristics on the duration and magnitude of these overvoltages has received considerable attention.[1–10] Furthermore, field tests have been made in order to determine the effectiveness of different methods of grounding, and the accuracy of the methods used to calculate the magnitude of overvoltage during system faults.[11–13]

It seems to be the proper time to consider and review the overvoltages produced by the occurrence of system faults, in order that full advantage of present-day knowledge may be taken by those concerned with system design and operation.

There are essentially three components of voltage due to the occurrence of a system fault:

1. Fundamental-frequency voltages.

2. Natural-frequency voltages usually of short duration which are superimposed upon the fundamental-frequency voltages.

3. Harmonic voltages resulting from unbalanced currents flowing in rotating machines in which the reactances in the direct and quadrature axes are unequal.

In general, protective devices, particularly lightning arresters, must be capable of withstanding the transient overvoltages to which they may be subjected for periods of short duration, and then must be able to seal off the power follow current associated with the sustained voltages. Unnecessary lightning-arrester failures due to excessive sustained overvoltages during fault may be prevented by properly grounding and operating the system.

It is the fundamental-frequency overvoltages which largely determine the lightning-arrester rating and the corresponding insulation protective levels. These in turn are a factor in determining the allowable apparatus insulation levels. From a technical standpoint, the fundamental-frequency voltages can be determined with fair accuracy.

Although this phase of the subject is of considerable importance, it has been discussed in the literature only in somewhat scattered and incomplete form. It is the purpose of this paper to review and analyze the factors which determine the maximum voltages which may be obtained on a system following the occurrence of faults, so that a more rational selection of system protective equipment can be made and the best method of system grounding determined.

Results of calculations, and of tests on a miniature system are presented. Included in these results are the transient voltages as well as the fundamental-frequency voltages that occur on a system during a fault. The information on transient overvoltages is of value in indicating the maximum magnitude of voltage that may be obtained on a system during a solid fault, thus showing the magnitude of transient voltage which is inherent in the circuit and which cannot be reduced by the elimination of excess voltages due to switching or arcing, or by building lightning-proof lines. Furthermore, as it seems reasonable to assume that arcing at the fault may subject the circuit during the arcing period to voltages of the same order of magnitude as those obtained immediately subsequent to the initial application of the fault, the curves presented can be used as a guide to indicate the maximum voltage which may be expected due to arcing across an insulator.

The paper analyzes the effect of those factors which determine the magnitude of the overvoltages during faults, which are the most common causes of high sustained voltages. A study of these voltages is usually a necessary first step, after which refinements and the more unusual cases can be considered. The effect of different methods of grounding and the influence of fault and arc resistance are included.

Conclusions

From the results discussed in this paper, the following conclusions can be drawn:

1. Line-to-ground faults for most systems can be used as a basis for determining the maximum fundamental-frequency and transient voltages during faults.

2. If a system is solidly grounded or grounded through reactance, so that the resultant zero-sequence impedance viewed from the fault is inductive rather than capacitive, the fundamental-frequency voltages-to-ground on the unfaulted phases at the fault will not be greater than 1.73 times normal line-to-neutral voltage.

3. If a system is grounded through resistance, so that the resultant zero-sequence impedance viewed from the fault is inductive rather than capacitive, the fundamental-frequency voltages-to-ground of the unfaulted phases at the fault will always be less than twice normal line-to-neutral voltage.

4. In order that the overvoltages during faults shall not exceed that which is considered safe for the operation of grounded-neutral lightning arresters, it is advisable that careful attention be paid to the method of grounding and the number of grounding points in the system. Consideration should also be given to the possibility of temporary disconnection of these grounding points and the resulting overvoltages which may occur.

5. Isolated-neutral systems may be subjected to particularly high overvoltages if the system is large in extent. The overvoltages at or near the region of resonance are appreciably reduced by fault and line resistance. This may be an important factor, particularly in low-voltage systems. On the other hand, resistance in the fault or ground return for a grounded-neutral system may slightly increase the overvoltages obtained on one of the open phases.

6. The transient overvoltages obtainable on a system which ordinarily might be considered to be solidly grounded may approach 2.73 times normal. Sustained voltages are generally higher for systems grounded through resistance than for systems grounded through corresponding values of reactance. The natural-frequency transient voltages obtained on a system grounded through resistance are practically negligible except when the resistance is high relative to the positive-sequence reactance.

7. The transient overvoltages during faults are not expected to be of sufficient magnitude to cause breakdown of the major insulation provided it is in good condition, except for the case of an isolated-neutral system having an appreciable amount of line or cable capacity to ground. The overvoltages during faults in a grounded-neutral system are not as great in magnitude as those which may be expected from lightning or switching surges.

8. Ground-fault-neutralizer (Petersen coil) systems are subjected to transient and fundamental-frequency overvoltages which are, in general, higher than those of a solidly grounded system but lower than the volt-

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Paper number 39-52, recommended by the AIEE committees on power transmission and distribution, and protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 12, 1938; made available for preprinting December 28, 1938.

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1. For all numbered references, see list at end of paper.

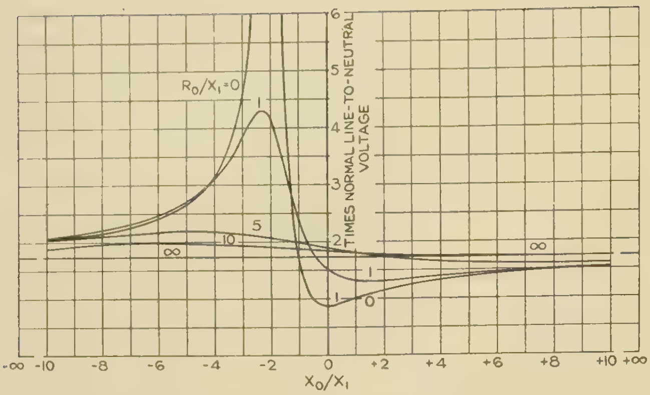


Figure 1. Fundamental-frequency voltages. Line-to-ground fault. Maximum voltage-to-ground of unfaulted phases at the fault

$$Z_1 = Z_2 = 0 + jX_1$$
$$Z_0 = R_0 + jX_0$$
$$R_f = 0$$

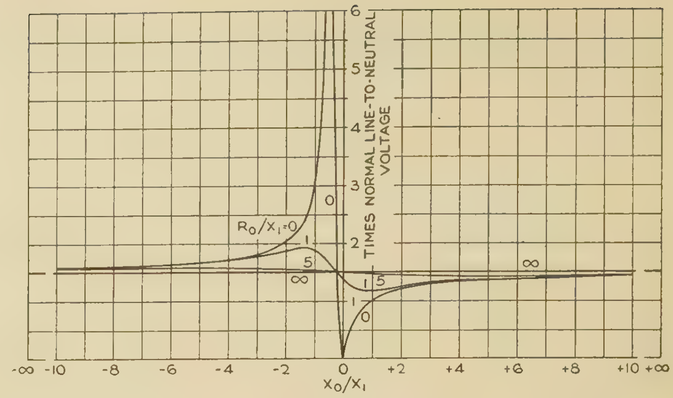


Figure 2. Fundamental-frequency voltages Double-line-to-ground fault. Voltage-to-ground of unfaulted phase at the fault

$$Z_1 = Z_2 = 0 + jX_1$$
$$Z_0 = R_0 + jX_0$$
$$R_f = 0$$

ages which can occur on an isolated-neutral system.

Basis of Study

The calculations, and tests on a miniature system, made to determine the magnitude of overvoltage following the occurrence of system faults, were based on the following assumptions and considerations:

1. The fault is a solid fault; that is, there is no arcing. Arcing tends to subject the circuit to a more or less continuous transient condition. It has been shown that very high voltage may be obtained in a circuit with no losses under assumed conditions of arc interruption and restriking.[7,14] Evidence indicates that these mechanisms of arcing do not ordinarily occur at the point of fault,[12,13] although such phenomena may be associated with switch operation.[15] It is believed that the maximum voltage which may be expected due to arcing can be taken to be approximately that corresponding to the transient overvoltage obtained immediately following the occurrence of a solid fault. Under this assumption the transient voltages presented in the paper are an indication of the maximum voltage which may be obtained due to arcing at the fault.[15]

2. Only fundamental and natural-frequency components of voltage are considered. Harmonics produced by the saliency effect of rotating apparatus are neglected. This assumes in effect that $x_q''/x_d'' = 1$. Exceptionally high voltages may occur due to saliency effects, particularly for the case of water-wheel generators not equipped with amortisseur windings and disconnected from their load. These overvoltages tend to be increased if the generator is left connected with an unbalanced fault to an appreciable amount of transmission line or cable so as to form a capacitive load. The conditions for this important exception are usually evident and are discussed fully in two recent papers.[5,6]

It is important to realize, however, that a certain amount of distortion due to this effect may occur for many system faults and that this will tend to increase the sustained voltages above those based only upon a consideration of the fundamental-frequency components.

3. Sudden disconnection of load and overspeeding of the system and connected generators is not considered. Loss of load on a generator may result in overvoltages due both to the sudden disconnection of the load and also to the overspeeding.[4] The results presented in this paper can, of course, be modified in order to indicate the increase in voltage which might be obtained under these conditions. This effect is most pronounced in portions of the system which overspeed due to loss of load on nearby hydro stations.

4. Negative-sequence impedances are assumed equal to positive-sequence impedances. The effect of rotating loads and machines, except in the case of synchronous machines not equipped with amortisseur windings, is to make the negative-sequence impedance less than the positive and to increase the losses with fault duration. These effects, in general, tend to reduce the magnitude of overvoltage calculated with equal positive- and negative-sequence impedances.

5. The magnitude of the positive-sequence impedance is assumed to be constant for the period in which the overvoltage is being determined. As the effective impedance of a rotating machine changes with time, it is necessary to consider the impedance under conditions which may result in the greatest overvoltage. On an isolated system, for example, the greatest overvoltage may be obtained based on impedances corresponding to the synchronous condition rather than the subtransient or transient condition. Because these impedances vary with time after the fault occurs, it is desirable that results be presented in the form of curves so that the range of voltage can be easily determined.

6. The effects of saturation and corona are not included. Both would tend to reduce

Figure 3. Fundamental-frequency voltages. Line-to-ground fault. Zero-sequence voltage at the fault

$$Z_1 = Z_2 = 0 + jX_1$$
$$Z_0 = R_0 + jX_0$$
$$R_f = 0$$

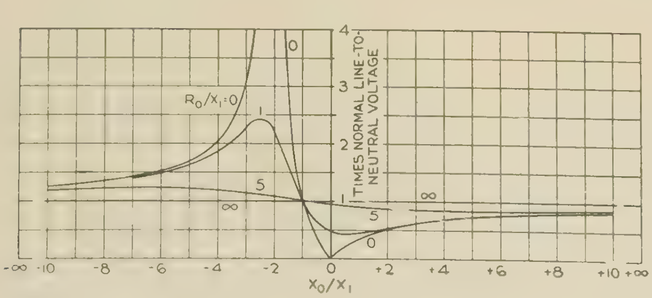
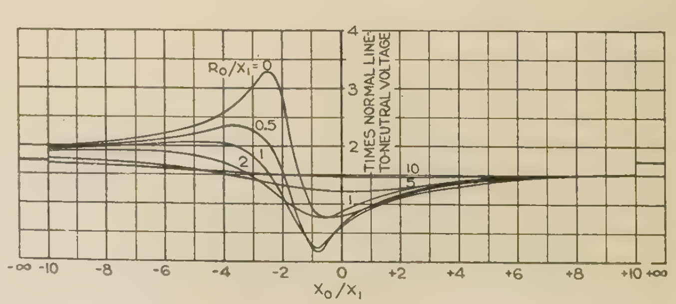


Figure 4. Fundamental-frequency voltages. Line-to-ground fault on phase a. Phase b voltage-to-ground at the fault

$$Z_1 = Z_2 = R_1 + jX_1$$
$$Z_0 = R_0 + jX_0$$
$$R_f = 0$$
$$R_1/X_1 = 0.4$$



the magnitude of overvoltage. The error in neglecting these factors, in general, increases with increase in the magnitude of overvoltage. In the region of resonance, saturation is an important factor in limiting the excessive fundamental-frequency overvoltages.

7. Overvoltages caused by unusual system conditions are neglected. For example, nonlinear circuit instability may be produced as a result of the opening of fuses or single-pole disconnect switches, thereby causing the magnetizing currents of potential or power transformers to flow through line capacitance with resulting overvoltages.

8. The miniature system used for determining transient overvoltages was set up to represent a low-loss system; that is, the ratio of r_1/x_1 was approximately 0.03. For systems having higher losses, the transient overvoltages will be somewhat less because of greater decrement factors.

9. Tests on the miniature system for determination of maximum transient voltage were made by representing the system by lumped impedance elements. This will, in general, lead to higher transient overvoltages at the point of fault than will be obtained in an actual system. This pessimistic result is due to the fact that the number of circuit natural frequencies is less than that on an actual system, and the resultant transient voltages are therefore likely to be of greater magnitude.

Discussion of Results

Fundamental-Frequency Voltages

Based on the foregoing assumptions, figure 1 gives the maximum fundamental-frequency voltage to ground which will occur on either of the unfaulted phases at the point of fault following a line-to-ground fault. Positive- and negative-sequence resistances are neglected, but zero-sequence resistance is included. Fault resistance is neglected. Figure 1 has an abscissa of X_0/X_1, where $Z_1 = 0 + jX_1$ and $Z_0 = R_0 + jX_0$ are the positive- and zero-sequence impedances, respectively, viewed from the fault. X_0 may be positive or negative. See equations 10 and 11, appendix A.

Figure 2, similar to figure 1, gives the maximum fundamental-frequency voltage on the unfaulted phase at the point of fault following a double-line-to-ground fault. Fault resistance and positive- and negative-sequence resistances are neglected. See equation 13, appendix A.

Figure 3 gives the zero-sequence voltage at the point of fault for a line-to-ground fault under the conditions of figure 1. The zero-sequence voltage at the point of fault for a double-line-to-ground fault under the conditions of figure 2, is one-third the voltage on the unfaulted phase given by figure 2. See equations 9 and 12, appendix A.

For a system which has its neutrals solidly grounded or grounded through reactance, the ratio X_0/X_1 will ordinarily be positive. That is, the zero-sequence impedance viewed from the fault is inductive rather than capacitive in effect. With neutrals grounded through resistance, X_0/X_1 may be either positive or negative. For an isolated-neutral system X_0/X_1 is negative. Also in a system which is extensive in number of miles of connected line or cable compared with the total admittance of the grounded points, the ratio of X_0/X_1 may be negative. With X_0/X_1 negative, the voltages obtained correspond to those on the left-hand side of the vertical axis. Under this condition, it is possible to obtain high overvoltages, particularly for small values of R_0/X_1 and values of X_0/X_1 in the region of resonance.

With all resistance neglected, infinite fault currents and voltages will occur if $X_0/X_1 = -2$ for a line-to-ground fault, and -0.5 for a double-line-to-ground fault. See appendix A, equations 9–13. Values of X_0/X_1 in the neighborhood of -2 or -0.5 are considered to be in the resonance region. Operation in the region of resonance with a small ratio of R_0/X_1 is expected to be unusual, and undoubtedly will occur only for isolated-neutral systems or grounded systems following the loss of the system ground point.

Figures 4 and 5, similar to figure 1, show the overvoltages which may be obtained on phases b and c, respectively, at the point of fault for the case of a single-conductor-to-ground fault on phase a. These two curves apply for $R_1/X_1 = 0.4$. Other curves similar to these have been prepared for early publication by E. M. Hunter with different ratios of positive-sequence resistance to reactance. This particular case is selected as being representative. As will be noted from these curves, the voltages on phase c are, in general, higher than those on phase b, although this is not true for all ratios of R_0/X_1 and X_0/X_1.

Figure 6, for the line-to-ground fault, shows the effect of fault resistance. Resistance except in the fault is neglected. As R_f/X_1 is increased from zero to $R_f/X_1 = 1$, approximately, the voltage of one of the unfaulted phases is increased and the other decreased, except in the region of resonance where both are decreased. The curve for $R_f/X_1 = 1$ is given in figure 6. As R_f/X_1 is further increased, the voltages decrease. Any increase in fault resistance decreases the overvoltage obtained in the region of resonance.

R_0 includes the effect of resistance in the ground return and in the neutral, while R_f of figure 6 is the resistance in the arc or fault. Figures 1, 2, 4, and 5 can be used to determine the magnitude of the fundamental-frequency fault voltages of systems with isolated neutral, neutral solidly grounded or grounded through resistance, reactance, or impedance. Z_0

Figure 5. Fundamental-frequency voltages. Line-to-ground fault on phase a. Phase c voltage-to-ground at the fault

$Z_1 = Z_2 = R_1 + jX_1$
$Z_0 = R_0 + jX_0$
$R_f = 0$
$R_1/X_1 = 0.4$

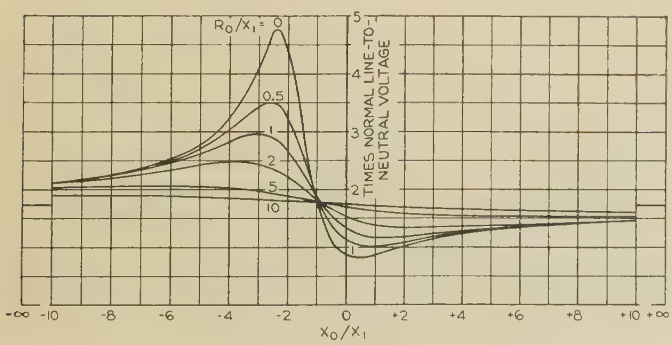
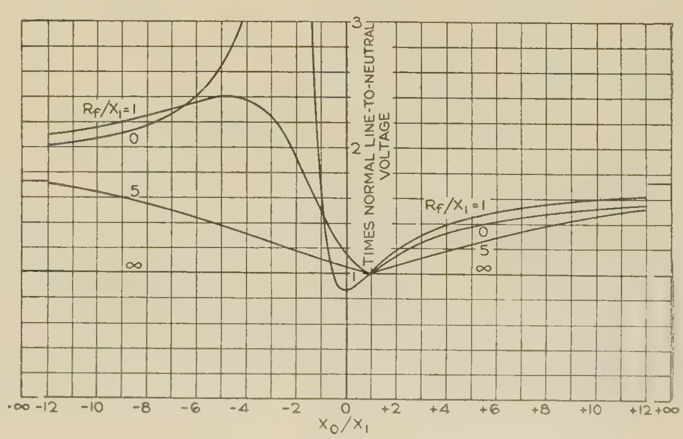


Figure 6. Fundamental-frequency voltages. Line-to-ground fault. Maximum voltage-to-ground of unfaulted phases at fault

$Z_1 = Z_2 = 0 + jX_1$
$Z_0 = 0 + jX_0$



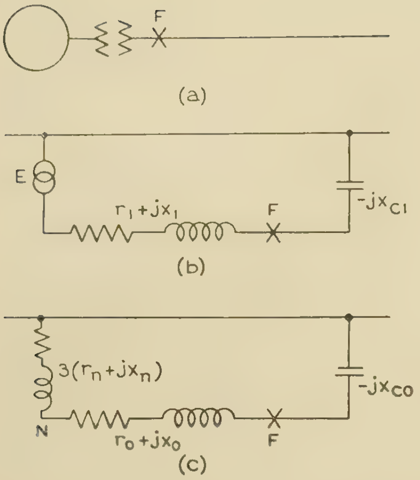


Figure 7

(*a*)—One-line diagram of power system consisting of synchronous machine, transformer bank, and transmission line

(*b*)—Approximate positive-sequence fundamental-frequency impedance diagram

(*c*)—Approximate zero-sequence fundamental-frequency impedance diagram

$= R_0 + jX_0$ is the zero-sequence impedance viewed from the fault and therefore includes the effect of neutral impedance.

Comparing corresponding curves in figures 1 and 2, it is seen that for the same system and fault location, that is, same values of X_0/X_1 and R_0/X_1, higher voltages are obtained for the line-to-ground than for the double-line-to-ground fault, except for the $R_0/X_1 = 0$ curve when X_0/X_1 lies between 1 and 5 and between -0.2 and -1.2, approximately. With X_0/X_1 between 1 and 5, and $R_0/X_1 = 0$, the voltages for the line-to-ground fault are but slightly less than those for the double-line-to-ground fault. As R_0/X_1 is increased, voltages increase more rapidly for the line-to-ground fault than for the double-line-to-ground fault. With $R_0/X_1 = 1$, higher values for the line-to-ground fault than for the double-line-to-ground fault are obtained. From equation 13, appendix *A*, any increase in R_1 reduces the voltage on the unfaulted phase for the double-line-to-ground fault; also, any increase in R_f reduces the voltage except when X_0/X_1 lies between -0.2 and 1.0.

Since resistance is always present, it can be concluded that with $Z_1 = Z_2$, the maximum fundamental-frequency voltage at the fault for the line-to-ground fault is as great as, or greater than, that for the double-line-to-ground fault, except for values of X_0/X_1 between -0.2 and -1.2, and very little resistance in the system. This represents a condition that is not expected to be met in practice except under the most unusual conditions.

Transient Voltages

Transient voltages here include both the fundamental-frequency component of voltage and the natural-frequency components. Curves for transient voltages following faults in terms of system impedances viewed from the fault cannot be drawn for the general case, as has been done for fundamental-frequency voltages. In the actual system, the transient voltages are affected by the number, connection, and arrangement of the circuits. To simplify the work, and give an indication of the maximum transient voltage to be expected, a system consisting of a synchronous generator, transformer bank, and transmission line open at the distant end was considered. With the fault on the line near the transformer terminals, as a first approximation, the open transmission line was replaced by its lumped capacitance at the point of fault. Figure 7, part *a*, gives a one-line diagram of the system studied; parts *b* and *c*, respectively, show the positive- and zero-sequence impedance diagrams for part *a*. Lowercase letters are used to differentiate the indicated impedances in figures 7 from the resultant effective impedances viewed from the fault which are represented by capitals.

In figure 7 the positive- and zero-sequence fundamental-frequency impedances viewed from the fault are

$$Z_1 = \frac{(r_1 + jx_1)x_{c1}}{x_{c1} - x_1 + jr_1} = R_1 + jX_1$$

$$Z_0 = \frac{[r_0 + 3r_n + j(x_0 + 3x_n)]x_{c0}}{x_{c0} - (x_0 + 3x_n) + j(r_0 + 3r_n)} = R_0 + jX_0$$

For a ground-fault neutralizer, $x_{c0} \cong x_0 + 3x_n$, and

$$R_0 \cong \frac{(x_0 + 3x_n)x_{c0}}{r_0 + 3r_n}$$

The coil is generally tuned so that X_0 has a very small positive value.

Resistance Neglected. With all resistance neglected in figure 7, transient voltages are expressed in appendix *A* in terms of the fundamental-frequency impedances viewed from the fault and the ratio of the positive to the zero-sequence capacitive reactance.

With resistance neglected, the fundamental-frequency impedances viewed from the fault in figure 7 are:

$$Z_1 = j\frac{x_1 x_{c1}}{x_{c1} - x_1} = 0 + jX_1$$

$$Z_0 = j\frac{(x_0 + 3x_n)x_{c0}}{x_{c0} - (x_0 + 3x_n)} = 0 + jX_0$$

With $(x_0 + 3x_n)$ greater than x_{c0}, X_0 is negative.

Figures 8 and 9 for line-to-ground and double-line-to-ground faults, respectively, give the maximum transient voltages in

Figure 8. Transient voltages with resistance neglected for system shown in figure 7. Line-to-ground fault. Maximum voltage-to-ground of unfaulted phases at the fault

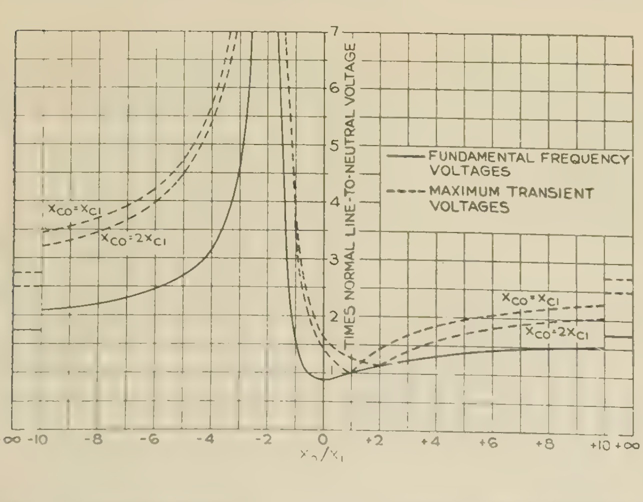
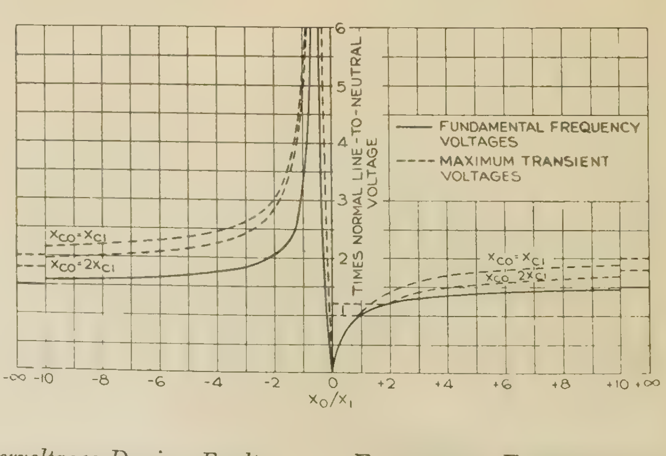


Figure 9. Transient voltages with resistance neglected for system shown in figure 7. Double-line-to-ground fault. Maximum voltage-to-ground of unfaulted phase at the fault



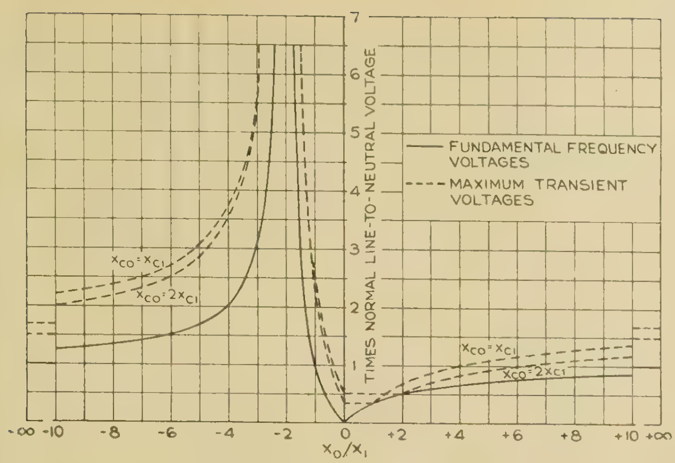


Figure 10. Transient voltages with resistance neglected for system shown in figure 7. Line-to-ground fault. Maximum zero-sequence voltage at the fault

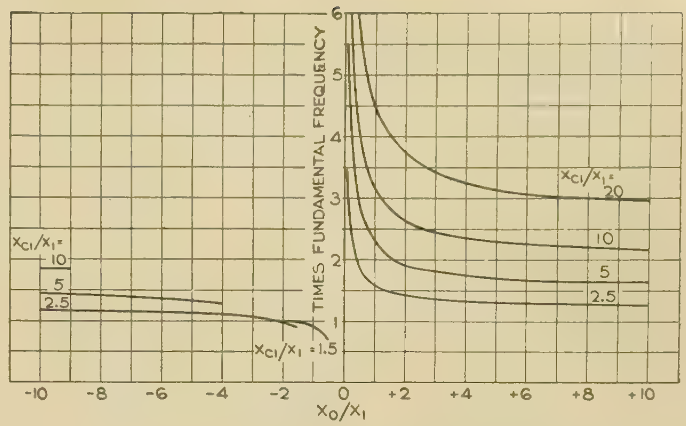


Figure 11. Natural frequency with line-to-ground fault for system shown in figure 7, with resistance neglected and $x_{c0}=x_{c1}$

per unit of normal peak line-to-neutral voltage on the unfaulted phases at the fault in terms of X_0/X_1 for the system shown in figure 7. The curves in figures 8 and 9 were calculated from equations 19 or 20 and 23, respectively, of appendix *A* with $\theta_0 = 90$ degrees. Since resistance is neglected there will be no decrement, and therefore the peak value of the fundamental-frequency term indicated in figures 8 and 9 by full-line curves, and the peak value of the natural-frequency term are added directly (since they must eventually come in phase) to give the total maximum voltage. Peak values of transient voltages are shown by dashed lines for $x_{c0} = x_{c1}$ and $x_{c0} = 2x_{c1}$.

Figure 10, for the same conditions as figure 8, gives the maximum transient zero-sequence voltage at the fault for a line-to-ground fault calculated from equation 18, appendix *A*. The maximum zero-sequence transient voltage at the fault for a double-line-to-ground fault will be one-third the voltage of the unfaulted phase given by figure 9. See equation 23, appendix *A*.

With X_0/X_1 between 0 and -2 in figure 8, and between 0 and -0.5 in figure 9, the natural frequency, ω_n, is less than unity. See equations 21 and 24, appendix *A*. Higher transient values than those plotted in this region could therefore have been obtained with $\theta_0 =$ 0 degrees instead of 90 degrees.

The natural frequency, ω_n, in per unity of fundamental frequency given by (21) for the line-to-ground fault with $x_{c0} = x_{c1}$ is plotted in figure 11, with abscissa X_0/X_1 and parameter x_{c1}/x_1. From figure 11, at the resonant point ($X_0/X_1 = -2$), the natural frequency is unity and for values of X_0/X_1 between -2 and 0, less than unity. The region between 0 and -2 corresponds to a very low ratio of x_{c1}/x_1, such as would probably not be encountered in a practical system operating normally.

Method of Grounding

Reactance Grounding

Figures 8 and 9 can be used to determine transient voltages with resistance neglected when the neutral is solidly grounded or grounded through any reactance, including a ground-fault neutralizer. For a solidly grounded neutral, $x_n = 0$. For a ground-fault neutralizer with resistance neglected, $3x_n + x_0 = x_{c0}$, and $X_0 = \infty$.

Comparison of Resistance and Reactance Grounding

A comparison of the effects of resistance and reactance grounding on the fundamental-frequency fault voltages following a line-to-ground fault can be obtained from figures 1, 4, and 5 when the positive- and zero-sequence impedances viewed from the fault are known.

To compare the effects of reactance and resistance grounding on transient overvoltages following faults, the curves of figure 8 for a line-to-ground fault have been replotted in figure 12 in terms of the impedances indicated in figure 7. Figures 12 and 13 show the magnitudes of the transient voltages which may be obtained following a line-to-ground fault when the neutral is grounded through reactance and through resistance, respectively, with $x_0 = x_1$ and $x_{c0} = x_{c1}$.

The voltages in figure 13 and some points in figure 12 were determined by using a miniature system having the characteristics and features described in appendix *B*. The full-line curves correspond to fundamental-frequency voltages for different values of the ratio of $3x_n/x_1$ or $3r_n/x_1$, where x_n is the grounding reactance in the case of figure 12 and r_n is the grounding resistance in the case of figure 13. As will be noted, exceptionally high voltages are not obtained until the capacitive reactance x_{c0} becomes small compared with x_1. This condition corresponds to a system having a very large amount of line-charging capacity compared with the connected generation. Also, it will be noted that transient voltages do not exceed twice the fundamental-frequency voltages with either reactance or resistance grounding, for the conditions assumed.

The sustained voltages for resistance-grounded systems are generally higher than those for corresponding reactance-grounded systems. This is particularly true if the neutral grounding ohms are selected to give the same value of short-circuit current, as can be seen from an analysis of figures 12 and 13. For high ohmic values of neutral-grounding impedance, the transient voltages may be higher for reactance-grounded than for resistance-grounded systems. For low ohmic values, the natural-frequency component of voltage for resistance-grounded systems decreases and becomes negligible for values of $3r_n/x_1 < 5$. Accordingly, a reactance-grounded system may not subject the protective equipment to as high sustained voltages as a resistance-grounded system, but from the standpoint of natural-frequency overvoltages, particularly those associated with switching phenomena, the resistance-grounded neutral may be more desirable.[15,16]

Figures 14 and 15 show the corresponding neutral-to-ground voltages for the cases shown in figures 12 and 13, respectively. This information is of value in indicating the voltages which may occur from neutral-to-ground under the condition of line-to-ground faults. Exceptionally, high overvoltages are not obtained at the neutral except for systems which have a relatively large amount of

connected line in proportion to the positive-sequence reactance. The curves, in general, have the same shape as those in figures 12 and 13, as it is essentially the shift or rise in neutral voltage which causes the overvoltage on the open phases.

Figures 16 and 17 are similar to figures 12 and 13, except that $x_{c0}/x_{c1} = 2$ in figure 16, and $x_0/x_1 = 0.5$ in figure 17. Only transient voltages are plotted in figures 16 and 17.

Since the magnitudes of overvoltages which are obtained are considerably affected by the method of grounding, this discussion is classified accordingly.

1. *Solidly Grounded System.* There are many degrees of *solid grounding* and the term means very little as far as the overvoltages are concerned, as it depends upon the number, location, and kilovolt-ampere capacity of the grounding points. However, if the grounded-neutral system is considered to be one in which grounded-neutral lightning arresters may be used (line-to-ground sustained voltages not to exceed 140 per cent to 150 per cent of normal), it becomes apparent that the ratio of X_0/X_1 should be kept below about 3 or 4. See figures 1, 2, and 6. Accordingly, this will ordinarily mean that a large percentage of all transformers or machines must be solidly grounded.

Systems operating in this classification, that is, X_0/X_1 not greater than 3 or 4, have a maximum transient line-to-ground voltage on the unfaulted phases not exceeding 2.0 times normal (see figures 8 and 9). The maximum neutral-to-ground transient voltage at any point in the system, for example, the neutral of an ungrounded bank, is about normal line-to-ground voltage due to the occurrence of a solid fault. See figure 10.

2. *Neutral Grounded Through Reactance.* When a system is grounded through reactance less than that of a ground-fault neutralizer, the zero-sequence impedance viewed from the fault is inductive rather than capacitive and the zero-sequence resistance is relatively small; accordingly, the fundamental-frequency phase-to-ground voltages will not exceed normal line-to-line voltage, and the neutral-to-ground voltage will not exceed normal line-to-neutral voltage. See figures 1–6, with X_0/X_1 positive and $R_0/X_1 < X_0/X_1$.

Following a fault, systems with reactance grounds will have maximum transient voltages to ground on the unfaulted phases not exceeding 2.73 times normal. The voltage to ground of the neutral will not exceed 1.67 times normal line-to-neutral voltage. See figures 8–10.

3. *Neutral Grounded Through Resistance.* When a system is grounded through resistance, the zero-sequence impedance viewed from the fault may be inductive or capacitive, depending upon the number and location of the grounding points and the amount of connected line or cable. With low-resistance grounds, X_0 will ordinarily be positive and the fundamental-frequency phase-to-ground voltages will, in general, not exceed normal line-to-line voltage and the neutral-to-ground voltages will not exceed normal line-to-neutral voltage. With high-resistance grounds, X_0 may be negative. In that case, phase-to-ground voltages may be greater than normal line-to-line voltages, and neutral-to-ground voltages greater than normal line-to-neutral voltages. See figures 1, 3, 4, and 5.

If low-resistance grounding is used, the natural-frequency voltages are practically eliminated and the maximum voltages are essentially the fundamental-frequency voltages which, however, are generally higher than the fundamental-frequency voltages obtained with corresponding values of neutral-grounding reactance.

4. *System Grounded Through Fault Neutralizer.* For systems grounded through ground-fault neutralizers, with resistance neglected, X_0 is infinite; with resistance included, R_0 is very large while X_0 is negative. Based on either assumption, the fundamental-frequency voltages on the unfaulted phases at the fault following a line-to-ground fault are essentially line-to-line voltages. See figure 1. The maximum transient voltages-to-ground on the unfaulted phases are less than 2.73 times normal, and of the neutral-to-ground less than 1.67 times normal line-to-neutral voltage. See figures 8 and 10.

Higher voltages may be obtained at points removed from the ground-fault neutralizers where there is in effect concentrated an appreciable amount of zero-sequence capacitance to ground. This indicates the advisability of placing the ground-fault neutralizer at the centers of the system, and also the desirability of using, under certain conditions, more than one ground-fault neutralizer.

5. *Isolated Neutral.* In the case of an isolated-neutral system, X_0 is negative and of the order of magnitude of the capacitive reactance while R_0/X_1 is relatively small. From figures 1, 4, and

Figure 12. Transient and fundamental-frequency voltages with resistance neglected for system shown in figure 7. Line-to-ground fault. Maximum voltage-to-ground of unfaulted phases at the fault

$x_0 = x_1$ $\qquad$ $x_{c0} = x_{c1}$

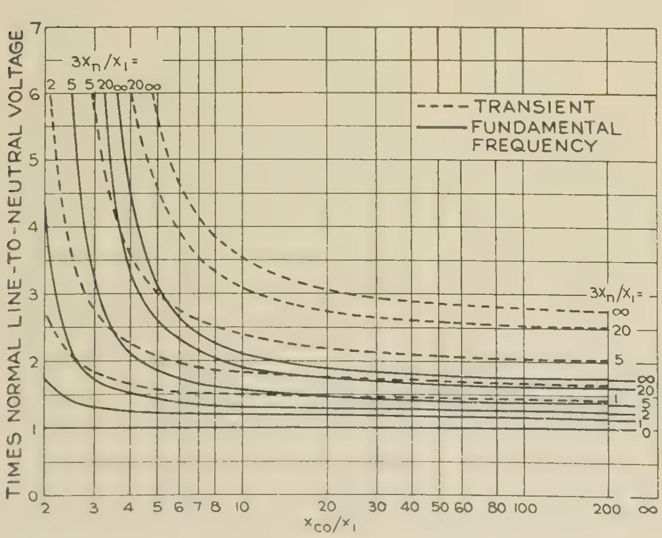
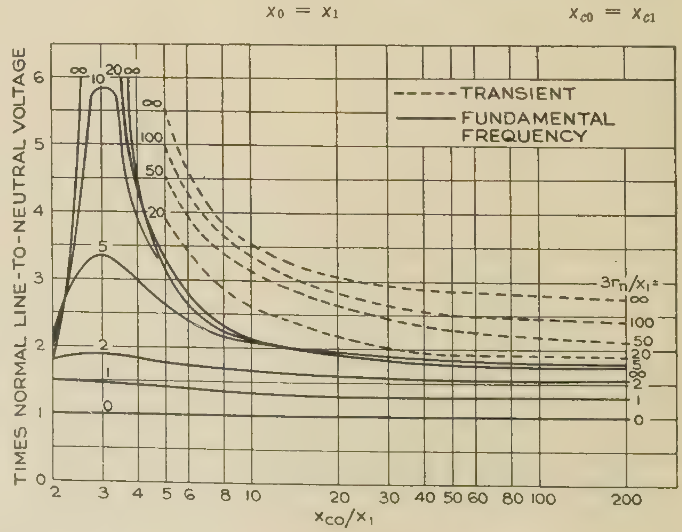


Figure 13. Transient and fundamental-frequency voltages for system shown in figure 7. Line-to-ground fault. Maximum voltage-to-ground of unfaulted phases at the fault

$x_0 = x_1$ $\qquad$ $x_{c0} = x_{c1}$



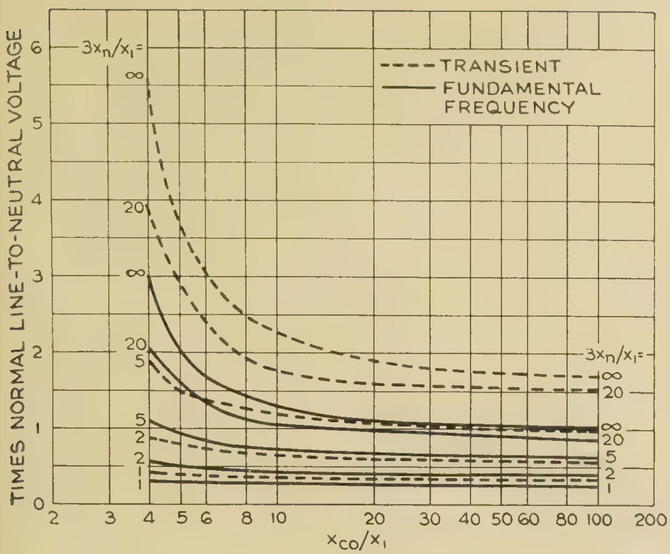


Figure 14. Transient and fundamental-frequency voltages with resistance neglected for system shown in figure 7. Line-to-ground fault. Maximum voltage-to-ground at the neutral

$x_0 = x_1$ $x_{c0} = x_{c1}$

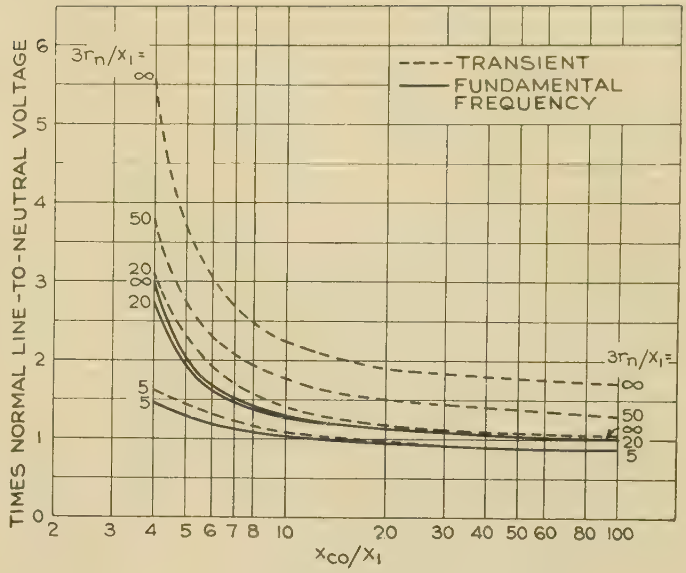


Figure 15. Transient and fundamental-frequency voltages for system shown in figure 7. Line-to-ground fault. Maximum voltage-to-ground at neutral

$x_0 = x_1$ $x_{c0} = x_{c1}$

5, the fundamental-frequency voltages obtained may be in excess of normal line-to-line voltage; and in some cases, particularly when the system is large in extent, considerably in excess, so that a very undesirable condition is created when faults occur. The fact that such voltages may be obtained makes it highly desirable that under no condition of operation shall a system lose its grounding points, if by so doing it is liable to be in the region of resonance. Otherwise, damage to the protective equipment or flashover of major equipment may result. The minimum voltage rating commonly used in isolated-neutral arresters is 1.83 times normal line-to-neutral voltage.

Voltages Distant From Fault

The fundamental-frequency and transient voltages given by the curves in this paper are at the point of fault. Under certain conditions higher voltages than those occurring at the point of fault may be obtained. This is particularly true at points which are located at a distance from the fault and the grounding points, but located where there is, in effect, an appreciable amount of zero-sequence capacitance to ground. The fundamental zero-sequence voltage at such points can be readily calculated from the zero-sequence voltage at the fault and the zero-sequence network. Figure 18 shows the simplified zero-sequence impedance diagram with the identity retained of the fault point F and point P, at which voltage is required. The zero-sequence voltage at P is

$$V_{a0} \text{ (at } P) = V_{a0} \text{ (at } F) \frac{Z_z}{Z_y + Z_z}$$

With Z_z capacitive reactance and Z_y inductive reactance, the zero-sequence voltage at P will be higher at P than at F. If it is appreciably higher, additional calculations are required for determining both fundamental- and natural-frequency voltages. The case of higher fundamental-frequency voltages at points distant from the fault than at the point of fault has been discussed in a recent paper.[9]

Nomenclature

$Z_1 = R_1 + jX_1$, $Z_0 = R_0 + jX_0$ = positive and zero-sequence fundamental-frequency impedances, respectively, viewed from the fault point

R_f = fault resistance

$Z_1(p)$, $Z_0(p)$ = operational expressions for the positive and zero-sequence impedances, respectively, viewed from the fault point

Lower case letters apply to the circuit shown in figure 7.

$z_1 = r_1 + jx_1$, $z_0 = r_0 + jx_0$ = positive and zero-sequence fundamental-frequency impedances, respectively, from neutral to the fault point

$z_n = r_n + jx_n$ = fundamental-frequency impedance between neutral and ground

x_{c1}, x_{c0} = positive and zero-sequence fundamental-frequency capacitive reactances, respectively, of transmission line

$\theta = \theta_0 + t$ = angle between direct axis of reference machine and axis of phase a

ω_n = natural frequency in times fundamental frequency

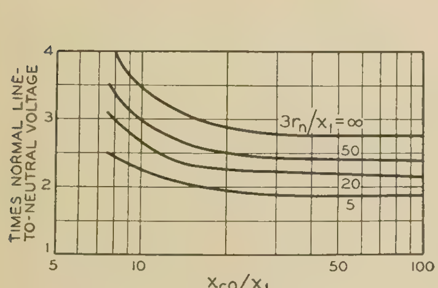


Figure 16. Maximum transient unfaulted-phase voltage-to-ground for system shown in figure 7. Line-to-ground fault. Neutral grounded through resistance

$x_0 = x_1$ $x_{c0} = 0.5x_{c1}$

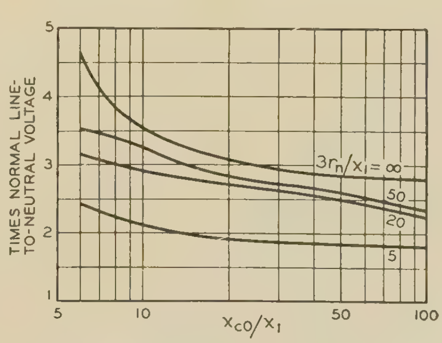


Figure 17. Maximum transient unfaulted-phase voltage-to-ground for system shown in figure 7. Line-to-ground fault. Neutral grounded through resistance

$x_0 = 0.5x_1$ $x_{c0} = x_{c1}$

Appendix A. Fundamental- and Natural-Frequency Voltages

The instantaneous phase voltages at any point in a balanced three-phase system in

per unit of crest voltages to neutral or to ground at that point are

$$\left.\begin{aligned} e_a &= -\sin\theta \\ e_b &= \frac{1}{2}\sin\theta + \frac{\sqrt{3}}{2}\cos\theta \\ e_c &= \frac{1}{2}\sin\theta - \frac{\sqrt{3}}{2}\cos\theta \end{aligned}\right\} \tag{1}$$

Let e and i with appropriate subscripts refer to instantaneous values of voltages-to-ground at the fault and currents flowing into the fault, respectively. In reference 17 are given the relations for transformation from three-phase quantities (a, b, c) to symmetrical-component quantities for the case of instantaneous currents and voltages, including the transient as well as the fundamental-frequency components. An application of these transformation relations is given in reference 5, except that α and β components are used. However, for the case under consideration, $Z_\alpha(p) = Z_\beta(p) = Z_1(p) = Z_2(p)$, so the application here is quite similar. Using the generalized method of symmetrical components, the following equations are obtained.

Line-to-Ground Fault (Phase a)

$$i_{a1} = i_{a2} = i_{a0} = \frac{-\sin\theta}{2Z_1(p) + Z_0(p) + 3R_f}$$

$$e_{a0} = -i_{a0}Z_0(p) = \frac{Z_0(p)\sin\theta}{2Z_1(p) + Z_0(p) + 3R_f} \tag{2}$$

$$e_b = \frac{1}{2}\sin\theta + \frac{\sqrt{3}}{2}\cos\theta + \frac{Z_0(p) - Z_1(p)}{2Z_1(p) + Z_0(p) + 3R_f}\sin\theta \tag{3}$$

$$e_c = \frac{1}{2}\sin\theta - \frac{\sqrt{3}}{2}\cos\theta + \frac{Z_0(p) - Z_1(p)}{2Z_1(p) + Z_0(p) + 3R_f}\sin\theta \tag{4}$$

Double Line-to-Ground Fault (Phases b and c)

$$i_{a0} = -(i_{a1} + i_{a2}) = \frac{\sin\theta}{Z_1(p) + 2Z_0(p) + 6R_f}$$

$$e_{a0} = -i_0Z_0(p) = \frac{-Z_0(p)\sin\theta}{Z_1(p) + 2Z_0(p) + 6R_f} \tag{5}$$

$$e_a = -\frac{3Z_0(p) + 6R_f}{Z_1(p) + 2Z_0(p) + 6R_f}\sin\theta \tag{6}$$

Line-to-Line Fault (Phases b and c)

$$e_a = -\sin\theta \tag{7}$$

$$e_b = e_c = \frac{1}{2}\sin\theta \tag{8}$$

Fundamental-Frequency Components of Voltage

Introducing vector quantities[5] by replacing $-\sin\theta$ by unity, $\cos\theta$ by $-j$, and $Z_1(p)$ and $Z_0(p)$ by Z_1 and Z_0, respectively, in (2)–(6), crest voltages of fundamental-frequency are obtained.

Line-to-Ground Fault (Phase a)

$$e_{a0} = -\frac{Z_0}{2Z_1 + Z_0 + 3R_f} \tag{9}$$

$$e_b = -\frac{1}{2} - j\frac{\sqrt{3}}{2} - \frac{Z_0 - Z_1}{2Z_1 + Z_0 + 3R_f} \tag{10}$$

$$e_c = -\frac{1}{2} + j\frac{\sqrt{3}}{2} - \frac{Z_0 - Z_1}{2Z_1 + Z_0 + 3R_f} \tag{11}$$

Double Line-to-Ground Fault (Phases b and c)

$$e_{a0} = \frac{Z_0}{Z_1 + 2Z_0 + 6R_f} \tag{12}$$

$$e_a = \frac{3Z_0 + 6R_f}{Z_1 + 2Z_0 + 6R_f} \tag{13}$$

Transient Voltages

To obtain the natural-frequency components of equations 2–6 necessitates replacing $Z_1(p)$ and $Z_0(p)$ by their operational expressions in terms of system constants. This can be done only when the system is given. For the system represented in figure 7,

$$Z_1(p) = \frac{(r_1 + px_1)\dfrac{x_{c1}}{p}}{r_1 + px_1 + \dfrac{x_{c1}}{p}} \tag{14}$$

$$Z_0(p) = \frac{[r_0 + 3r_n + p(x_0 + 3x_n)]\dfrac{x_{c0}}{p}}{r_0 + 3r_n + p(x_0 + 3x_n) + \dfrac{x_{c0}}{p}} \tag{15}$$

Resistance Neglected. With resistance neglected in figure 7, $Z_1(p)$, $Z_0(p)$, Z_1, and Z_0 become,

$$Z_1(p) = \frac{px_1x_{c1}}{p^2x_1 + x_{c1}} \tag{14a}$$

$$Z_0(p) = \frac{p(x_0 + 3x_n)x_{c0}}{p^2(x_0 + 3x_n) + x_{c0}} \tag{15a}$$

$$Z_1 = \frac{jx_1x_{c1}}{x_{c1} - x_1} = jX_1 \tag{16}$$

$$Z_0 = j\frac{(x_0 + 3x_n)x_{c0}}{x_{c0} - (x_0 + 3x_n)} = jX_0 \tag{17}$$

Line-to-Ground Fault. With a line-to-ground fault at F on phase a in figure 7, the instantaneous zero-sequence voltage at the fault in per unit of normal line-to-neutral peak voltage at the fault, obtained by substituting (14a) and (15a) in (2) and solving the operational equations is

$$e_0 = \frac{Z_0}{2Z_1 + Z_0}\sin\theta - \frac{2\left(\dfrac{x_0 + 3x_n}{x_{c0}} - \dfrac{x_1}{x_{c1}}\right)}{\left(\dfrac{2}{x_{c0}} + \dfrac{1}{x_{c1}}\right)\left[2x_1 + x_0 + 3x_n - x_1(x_0 + 3x_n)\left(\dfrac{2}{x_{c0}} + \dfrac{1}{x_{c1}}\right)\right]} \times \left(\sin\theta_0\cos\omega_nt + \frac{1}{\omega_n}\cos\theta_0\sin\omega_nt\right)$$

In terms of X_0 and X_1,

$$e_{a0} = \frac{X_0}{2X_1 + X_0}\sin\theta - \frac{2\left(\dfrac{x_{c1}}{x_{c0}}X_0 - X_1\right)}{\left(1 + 2\dfrac{x_{c1}}{x_{c0}}\right)(2X_1 + X_0)} \times \left(\sin\theta_0\cos\omega_nt + \frac{1}{\omega_n}\cos\theta_0\sin\omega_nt\right) \tag{18}$$

with $x_{c0} = x_{c1}$, equation 18 checks that given by Fallou.[10]

$$e_b = \frac{3}{2}e_0 + \frac{\sqrt{3}}{2}\cos\theta \tag{19}$$

$$e_c = \frac{3}{2}e_0 - \frac{\sqrt{3}}{2}\cos\theta \tag{20}$$

where

$$\omega_n = \sqrt{\frac{2x_1 + x_0 + 3x_n}{2x_1(x_0 + 3x_n)\left(\dfrac{1}{x_{c0}} + \dfrac{1}{2x_{c1}}\right)}} = \sqrt{\frac{\dfrac{X_0}{X_1}\left(\dfrac{x_{c1}}{x_1} + 2\dfrac{x_{c1}}{x_{c0}}\right) + 2\left(\dfrac{x_{c1}}{x_1} - 1\right)}{\dfrac{X_0}{X_1}\left(2\dfrac{x_{c1}}{x_{c0}} + 1\right)}} \tag{21}$$

Double-Line-to-Ground Fault. With a double-line-to-ground fault at F on phases b and c, the instantaneous zero-sequence and phase a voltage-to-ground at the fault in per unit of normal line-to-neutral peak voltage at the fault are obtained by substituting (14a) and (15a) in (5) and (6) and solving the operational equations. Expressed in terms of the positive- and zero-sequence reactances viewed from the fault,

$$e_0 = -\frac{X_0}{X_1 + 2X_0}\sin\theta + \frac{\dfrac{x_{c1}}{x_{c0}}X_0 - X_1}{\left(2 + \dfrac{x_{c1}}{x_{c0}}\right)(X_1 + 2X_0)} \times \left(\sin\theta_0\cos\omega_nt + \frac{\cos\theta}{\omega_n}\sin\omega_nt\right) \tag{22}$$

$$e_a = 3e_0 \tag{23}$$

where

$$\omega_n = \sqrt{\frac{x_1 + 2(x_0 + 3x_n)}{2x_1(x_0 + 3x_n)\left(\dfrac{1}{2x_{c0}} + \dfrac{1}{x_{c1}}\right)}} \tag{24}$$

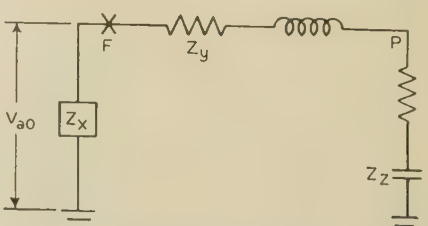


Figure 18. Simplified zero-sequence impedance diagram

Appendix B. Description of Miniature System

The miniature equivalent system used in making the laboratory tests is shown in figure 19.

The reactors used in this system had low ratios of resistance to reactance which varied from about 0.01 to 0.04 at 60 cycles depending upon the portion of the total winding being used. The miniature power

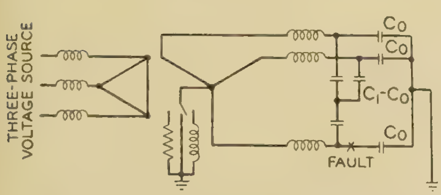


Figure 19. Miniature equivalent system

transformer was also of low-loss design so that the entire system caused relatively low damping of natural-frequency oscillations. This was desirable for purposes of checking calculations based on no-loss circuits.

The miniature system was energized from a three-phase 110-volt 60-cycle voltage source of such capacity that its terminal voltage remained essentially constant regardless of transient conditions imposed by faults in the equivalent circuits.

A synchronous commutator was used to apply and remove repeatedly the fault in synchronism with the system voltage. The commutator drum was driven by an 1,800-rpm synchronous motor by means of a ten-to-one gear reduction so that one revolution of the drum was completed every 20 cycles. The contacts on the switch were so made that during these 20 cycles the fault would be on 5 cycles and off 15 cycles. The relatively long period during which the fault was off afforded ample time for steady-state conditions to be reached before the switching operation was repeated.

A cathode-ray oscilloscope was used to measure the transient voltages. Another contactor on the synchronous commutator was used to control the grid on the cathode-ray tube so that the light beam would appear on the screen for only one cycle out of each 20. This made it possible to obtain a clear picture of the transient voltages during the period of particular interest.

The contactors on the commutator were mounted on a rack which could be rotated to vary the instant of application of the fault corresponding to various points on the voltage wave. Thus it was possible to determine the maximum voltage that could be reached by selecting the angle of fault application which gave the highest transient voltage on the oscilloscope.

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15. EFFECT OF RESTRIKING ON RECOVERY VOLTAGE, **C. Concordia** and **W. F. Skeats**. AIEE TRANSACTIONS, volume 58, 1939, pages 371–6 (August section).

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Discussion

P. A. Jeanne: See discussion, page 395.

R. D. Evans, A. C. Monteith, and **R. L. Witzke** (all of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Concerning the results in the Clarke-Crary-Peterson paper, it may be observed that the simple form of network illustrated in figure 19 for transient analysis, might lead to appreciable error. The use of such a simple equivalent circuit reduces to an extent the usefulness of the results.

Also, in connection with this paper, the authors have carried the curves to negative value of X_0/X_1 whereas in the previous published work by Evans and Wright[1] only values for positive ratios were considered. The curves in the negative zone are no doubt correct from an academic point of view. However, we are inclined to question their practical use when high values of overvoltage are considered as the constants of the circuit will change in a direction tending to limit the magnitude of voltage. Such changes in circuit constants may be due to corona, magnetizing current of transformers, etc. The practical range of the curves is probably from −1 ratio to the positive values.

A value of X_0/X_1 of 3 to 4 is suggested for a safe application of a grounded-neutral lightning arrester. It is believed that in drawing this conclusion the effects of fault resistance have been ignored. When high values of fault resistance are considered the ratio of X_0/X_1 must be lowered if the fault is at the point of application of the arrester. In the work published by Monteith and Roman[2] it was considered improbable to have a fault at the point of application of the arrester since the arrester should prevent flashover. When the fault is considered at a point other than where the arrester is applied, the ratio of 3 to 4 for X_0/X_1, can be selected and the criterion will be entirely independent of fault resistance.

(See also discussion, page 412.)

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Edith Clarke, S. B. Crary, and **H. A. Peterson:** In answer to the discussion by Messrs. Evans, Monteith, and Witzke:

The simple network of figure 19, although an approximation, gives transient voltages at the point of fault not *less* than the maximum transient voltages which may occur (see "Basis of Study," 9), and thus enables us to draw conclusions in regard to the maximum transient voltages which can occur at the point of fault for various conditions of grounding.

The curves for negative values of X_0/X_1 have been found practical in determining voltages corresponding to high negative values of X_0/X_1 beyond the resonance region. As pointed out in the paper, the very high voltages indicated by the curves would not be obtained in the resonance region, since they are critical values and therefore greatly influenced by saturation, corona, and variation of machine reactances with time (see "Basis of Study," 5 and 6). They are useful, however, in that they indicate regions where high voltages will be obtained, that is, voltages in excess of normal line-to-line voltages.

The effect of fault resistance was not ignored in suggesting a value of X_0/X_1 between 3 and 4 for safe application of a grounded-neutral arrester. This conclusion is based on the curves of figure 6, which include the effect of fault resistance. The curves apply to cases in which the arrester and fault locations are far enough from each other so that mutual ground resistance of the arrester and fault is not encountered, and yet not so remote from each other that voltages to ground at the arrester are appreciably different from those indicated. Figure 6 in the region of $X_0/X_1 = 1$ to 5 checks the curves of figure 4 given by Messrs. Monteith and Roman in reference 8.

Power-System Transients Caused by Switching and Faults

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Synopsis: This paper summarizes the results of an investigation of transient voltages on power systems caused by switching and faults. The transient voltages on power systems as measured by the "klydonograph" are reviewed and compared with the flashover values of transmission-line insulation. It is shown that the higher values of transient voltages are produced by intermittent arcs. In part I, the various theories for the production of transient voltages of high magnitude as a result of intermittent arcs are reviewed and extended in order to obtain the highest voltages on typical polyphase systems with the range of natural frequencies and attenuation factors that are encountered in practice. Previous studies are of limited scope and apply principally to the case of an arcing ground on an ungrounded system. The present study shows broadly the range of transient voltages which may be produced with intermittent arcs and applies to switching operations as well as arcing grounds. A typical transmission system is studied with the aid of the a-c network calculator. One of the principal variable factors in this study is the method of system grounding and this includes a range of both resistance and reactance between the limits of a solidly grounded system and an ungrounded system. The study is carried out for four different conditions, namely: (1) arcing grounds, (2) de-energizing an unfaulted line section, (3) de-energizing a line section with a fault on one phase, and (4) de-energizing a line section with a fault on two phases. The results of this study are presented in graphical form in part II and show many interesting properties of systems with respect to the method of grounding, and the characteristics of transient voltages for the different switching and fault conditions. It is the authors' opinion that the transient voltages due to faults and switching deserve more attention than they have received within the last few years.

THE EXISTENCE of transient overvoltages on transmission (and distribution) systems as a result of circuit changes caused by switching operations or faults has long been recognized. Many years ago when transmission systems were first expanding, the effect of arcing grounds received a great deal of attention. The phenomena, however, were not thoroughly investigated at the time because suitable measuring and recording devices were not available and because the immediate difficulties were largely overcome by the adoption of the practice of grounding transmission systems. The invention by J. F. Peters[1] of the "klydonograph," the first really practical surge recorder, made possible the collection of a mass of field data on overvoltages. The introduction of this instrument stimulated extensive investigations of voltages caused both by lightning and other transients. In recent years, considerable attention has been directed to the lightning phase of the problem but other phases have largely been neglected. It is the authors' view that the time has come when further attention should be given to the problems of overvoltages caused by faults and switching operations. To them it seems quite possible that some of the multiphase faults on systems which are now attributed wholly to lightning may, in reality, be caused in part by voltages produced by intermittent arcs.

It is pertinent to review the operating experience which has been obtained on transmission lines in regard to overvoltages produced by switching surges arising from circuit changes or isolation of faulted conductors. Quite a number of klydonograph investigations have been reported in the literature and many of these segregate the overvoltages resulting from switching operations from those due to lightning. Extensive investigations were reported by Cox, McAuley, and Huggins;[2] Gross and Cox;[3] Lewis and Foust;[4] and by a number of European investigators. The Joint Subcommittee on Development and Research of the Edison Electric Institute and Bell Telephone System, has also carried on some investigations and has made an excellent summary[5] of the more important published data.

The principal results of the switching surge studies made with the aid of the klydonograph have been summarized in figure 1. In this figure, curves A and B obtained from the original investigation by Cox, McAuley, and Huggins,[2] give the voltages due to energizing or de-energizing operations and the voltages due to faults with subsequent switching, respectively. Curve C gives a summary derived from the work of Lewis and Foust,[4] the most recent report of its kind. In order to give a more suitable scale for plotting the results of the surge studies all the surges of a magnitude less than twice normal have been disregarded. The Lewis and Foust paper, however, shows that of all the reported surges above normal voltage, 45 per cent were above twice normal. Figure 1 shows that the limiting value of the surges is about six times normal crest voltage, 5 per cent exceed five times normal, and 20 per cent exceed four times normal.

It will be noted from figure 1 that there is an upper limit to the voltage recorded, indicating the possibility of some limiting factor. Figure 2 shows the ratio of the voltage required to produce flashover, to the normal crest voltage, for different voltage transmission lines equipped with

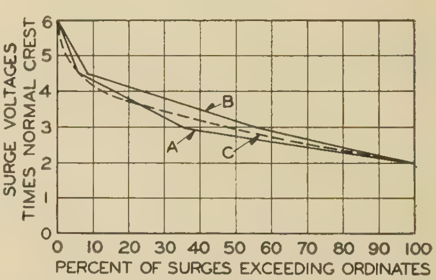

Figure 1. Distribution of surge voltages caused by switching and faults

A—Switching surges—Cox, McAuley, and Huggins

B—Surges from faults—Cox, McAuley, and Huggins

C—Switching surges—Lewis and Foust

A and B—Eighteen systems—1925 to 1926

C—Fourteen systems—1926 to 1930

the range of insulator units encountered in practice. The shape of the curve of figure 1 compared with the data given in figure 2, indicates that the magnitude of switching surges recorded could be limited by line flashover. While it is undoubtedly true that a considerable percentage of these switching operations take place with relatively little energy in the oscilla-

Paper number 39-48, recommended by the AIEE committees on power transmission and distribution, and protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 14, 1938; made available for preprinting December 27, 1938.

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The authors wish to acknowledge the assistance which they have received from their associates, particularly I. F. Krughoff, who carried out the principal part of the studies with the a-c network calculator.

1. For all numbered references, see list at end of paper.

tion and at relatively high frequency, it is also true that as systems expand the natural frequency of systems for switching operations decreases and the amount of energy in these oscillations increases. Thus, these factors tend to increase the importance of switching surges.

Part I. Mechanism of Producing Transient Overvoltages as a Result of Circuit Changes

The mechanism of producing transient overvoltages on transmission (and distribution) systems as a result of circuit changes caused by switching operations and faults will now be considered. The magnitude of these transient voltages, as shown in figure 1, will, under some conditions, exceed the sum of two components, (1) the final steady-state voltage, and (2) a transient voltage equal to the difference between it and the initial steady-state voltage. The value of this transient voltage has been assumed to be the one indicated by the conventional theory of circuit changes involving a single switching operation producing a simple circuit change such as a "make" or a "break." Thus the transient voltage, which is due to a "make" such as the sudden and permanent fault to ground, or a "break" such as the sudden and permanent de-energizing of a circuit, are relatively low in comparison with the maximum transient voltages that are known to have been produced by switching operations or faults. In a "simple circuit change" as the expression is used in this paper, it is assumed that the change caused by a fault or switching operation takes place through a path that changes abruptly from a perfectly conducting to a nonconducting condition, or vice versa.

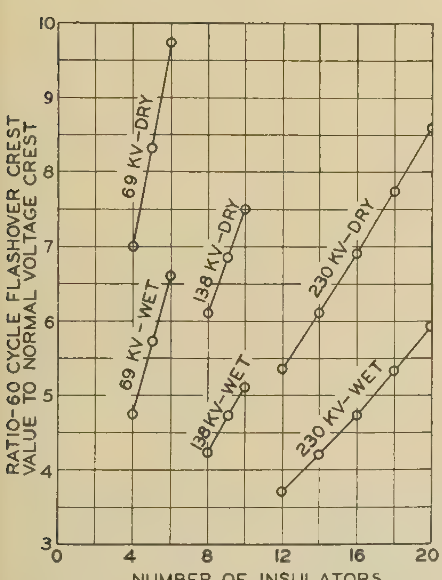


Figure 2. Sixty-cycle flashover voltage ratios for ten-inch suspension units

It is known that the circuit changes which produce the higher values of transient voltages involve arc paths. It has also been recognized that the characteristics of the arc path have a relation to the overvoltages, but the nature of the phenomenon has, in general, not been well understood. A little consideration will show that the conditions for producing high transient overvoltages from switching or faults have in common these features of (1) a circuit change through a path involving an arc, and (2) a circuit change which differs from that of a single "make" or "break" as previously discussed.

In considering the characteristics of an arc path in respect to the production of transient overvoltages, two classes of factors may be recognized, namely:

1. Abrupt forcing of current zero and high extinction voltage.
2. Cumulative action from intermittent arcing.

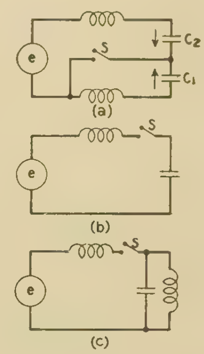
The phenomenon involving the first of these, the abrupt forcing of current zero, is well known in the simple form by which high voltages are produced when inductive circuits are quickly opened. This is not likely to be encountered with conventional types of interrupting equipment for faults and switching operations on power transmission systems. Instead, the probable conditions for the production of overvoltages from the first factor include the opening of the magnetizing circuits of transformers, switching of induction regulators, and other similar operations.

The extinction voltage or the voltage across an arc just prior to its interruption is also a factor affecting the magnitude of transient overvoltages. This factor is of importance on low-voltage but not on high-voltage circuits. In general, it is the authors' opinion, as pointed out elsewhere,[6] that the factors of abrupt forcing of current zero and high extinction voltage are not responsible for producing the more important transient overvoltages on transmission systems. Accordingly, only the effect of cumulative action from intermittent arcing has been considered in this investigation.

Theories of Intermittent Arcing

Various theories of intermittent arcing have been proposed to account for the high voltages produced by arcing grounds on ungrounded systems. These theories were reviewed some 15 years ago by Peters and Slepian[7] and their designations for the principal ones as theory I, theory II, and theory III, have been retained. The application of these theories has been extended in this investigation to the full range of grounding conditions for the arcing ground case and to other cases involving switching and faults. These theories have been studied for simple and complicated circuits and it has been found that the basic features of theories I, II, and III are applicable but that to produce the highest voltages some modifications were necessary to cover the range of natural frequencies and attenuation factors, and the complication of circuits encountered on actual three-phase systems.

Figure 3. Simple circuits illustrating arcing grounds (a) and switching surges (b, c)



The close relationship between the intermittent arcing theories for arcing ground and switching cases makes it desirable to consider them together. The various theories of intermittent arcing are based on different assumptions in regard to the points at which the arc is interrupted and established or re-established. Thus the interruption of the arc may take place at a current zero close to a fundamental current zero or close to a natural- or high-frequency current zero. The arc may be established or re-established at a fundamental-frequency voltage crest or at a natural-frequency voltage crest.

In explaining these theories it is convenient to represent the intermittent arc by a switch, the opening and closing of which are controlled in accordance with the different theories. In using the switch to simulate an intermittent arc, certain dielectric characteristics of the arc path are assumed as discussed subsequently.

The essential features of the three theories are illustrated with the aid of the simplified circuits in figure 3. Figure 3*a* is a circuit for the representation of an arcing ground taking place at point *S*.

Figure 3*b* is a circuit used to represent the switching of a capacitor when supplied from a source through an inductance and corresponds to a switching operation where the disconnected section is left without a discharge path. Figure 3*c* is

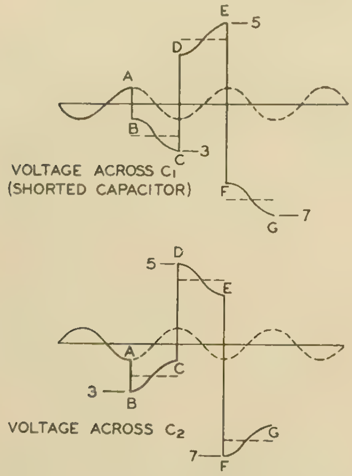


Figure 4a. Arcing ground (circuit 3a, theory III)

Switch closed at *A*, *C*, *E*, *G*
Switch opened at *B*, *D*, *F*

similar to the previous circuit but differs in that the disconnected section is provided with a discharge path.

The distinguishing features of the three theories of intermittent arcing may be obtained most readily by reference to table I. Part *a* of this table applies to the case of energizing a circuit, such as initiating an arcing ground in the circuit of figure 3*a* or the closing of a line section in the circuits of figures 3*b* and 3*c*. In all three theories the first closing of the switch is assumed to take place at the crest of the fundamental-frequency voltage across the switch, as this gives the highest transient voltages. In theories I and III the switch is always opened at the first current zero which is determined primarily by the natural- or high-frequency component of the current. In theory II the switch is not opened until the first fundamental current zero. In theory I the arc is assumed to restrike at the crest of the high-frequency oscillation across the switch. This restrike is simulated by reclosing the switch *S*. In theories II and III, the restrike takes place at the crest of the fundamental component of voltage across the switch.

Table I. Comparison of Theories of Intermittent Arcing

(a) Initial Condition—Circuit Open

Theory	First Close*	First Open**	First Restrike*	Second Open**	Second Restrike*
I	FF	NF	NF	NF	NF
II	FF	FF	FF	FF	FF
III	FF	NF	FF	NF	FF

(b) Initial Condition—Circuit Closed

Theory	First Open**	First Restrike*	Second Open**	Second Restrike*	Third Open**
I	FF	NF	NF	NF	NF
II	FF	FF	FF	FF	FF
III	FF	FF	NF	FF	NF

* Closing occurs at a voltage crest of FF or NF component.

** Opening occurs at a current zero, determined principally by the FF or NF component.

FF = Fundamental frequency.

NF = Natural or high frequency.

In the second part of table I, the switching sequences are illustrated for the case of de-energizing a circuit. In this case the switch *S* is initially closed, and is opened at a fundamental current zero. After the first opening the restrikes and arc extinctions follow the same sequence as for energizing a circuit.

In order to clarify the above theories several cases of intermittent arcing are discussed in detail for the circuits of figure 3. First, the case of de-energizing the capacitor in the circuit of figure 3*b* is analyzed in accordance with theory III. The successive steps of building up the capacitor voltage are shown in figure 4*b*. Normal capacitor voltage has been added as a dotted curve. The capacitor voltage is normal and at point *A* the switch is opened and a charge is left on the capacitor. The switch voltage is now the algebraic sum of the generated voltage and the voltage due to the charge on the capacitor. It will reach a maximum of twice normal generated voltage crest at one-half cycle of fundamental frequency after the switch is opened. At the point *B* the capacitor voltage has a value of +1 in per unit values, whereas the normal capacitor voltage, with the switch closed, is −1. Now if the switch is closed at this time the capacitor voltage will be accelerated toward a value of −1 but because of the circuit inductance or inertia the voltage will overshoot and, without damping, will reach a value of −3. If now the switch is opened, the capacitor will have a charge corresponding to a voltage of −3. If the switch is again reclosed one-half cycle after opening, the voltage will be accelerated from −3 toward a value of +1 but will overshoot to a

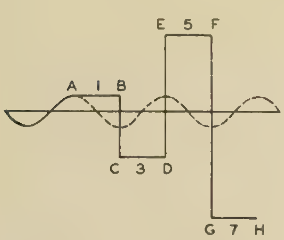


Figure 4b. De-energizing a line section (circuit 3b, theory III)

Switch opened at *A*, *C*, *E*, *G*
Switch closed at *B*, *D*, *F*, *H*

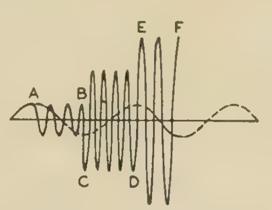


Figure 4c. De-energizing a line section (circuit 3c, theory III)

Switch opened at *A*, *C*, *E*
Switch closed at *B*, *D*, *F*

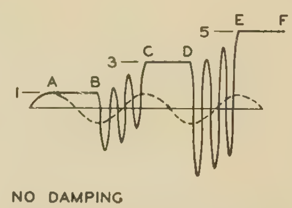


Figure 4d. De-energizing a line section (circuit 3b, theory II)

Switch opened at *A*, *C*, *E*
Switch closed at *B*, *D*, *F*

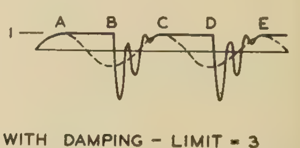


Figure 4e. De-energizing a line section (circuit 3b, theory II)

Switch opened at *A*, *C*, *E*
Switch closed at *B*, *D*

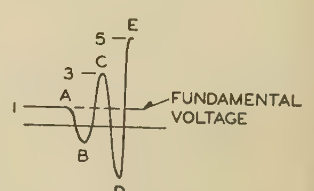


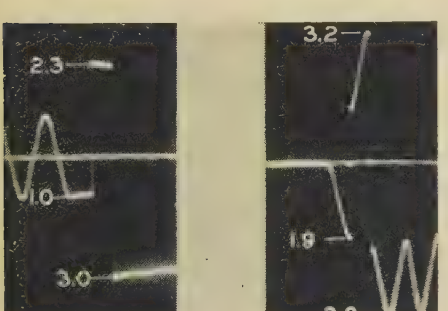
Figure 4f. De-energizing a line section (circuit 3b, theory I)

Switch opened at *A*, *C*, *E*
Switch closed at *B*, *D*

value of +5. It can be seen that the voltage builds up according to the series, 1, 3, 5, 7, . . . and will have no limit if damping is neglected. In this analysis it has been assumed that the inductance is small and that the natural frequency of the circuit is very high in comparison to the fundamental frequency.

Theory III has also been illustrated in figure 4*c* for the circuit of figure 3*c*. This case differs from that of figure 4*b* only in that the capacitor voltage oscillates around zero during the time the switch is open. The capacitor voltage is assumed to be the same in both cases at the time the switch is reclosed. The curves are derived on the assumption of no damping in the circuit.

In figure 4*a* are shown the voltages across the capacitors C_1 and C_2 of the circuit shown in figure 3*a*. In this case an arcing ground is simulated by opening and closing the switch *S* in accordance with theory III. The capacitors are assumed to be of equal capacitance and the circuit inductance is very low, so that one-half the generated voltage appears across each capacitor with the switch open. If the switch is closed at *A*, the voltage across capacitor C_1 oscillates around zero because the generated voltage is not impressed on this mesh. With C_1 short-circuited, the steady-state voltage across C_2 is twice normal. Therefore, when the switch *S* is closed, capacitor C_2 is accelerated toward twice normal but will overshoot to three times normal. Now assume that the switch is opened at *B*, at which time C_1 has a charge corresponding to a voltage -1 and C_2 has a charge corresponding to a voltage -3. Because these charges are in opposite directions around the circuit through the generator, the capacitors will not discharge but will equalize with a charge corresponding to a voltage of -2. The steady-state voltage across the capacitor will then be the algebraic sum of one-half the generated voltage, and the voltage due to the charge on

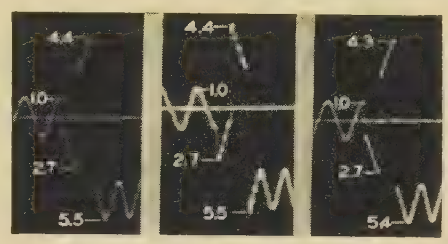


(*a*)—Capacitor voltage (*b*)—Switch voltage

Figure 5. De-energizing a line section (circuit 3b, theory III)

the capacitor. By referring to figure 4*a* it can be seen that the voltage across both capacitors is equal to these steady-state values at the instant the switch is opened and as the initial and steady-state voltages are equal, there will be no transient.

If the switch is reclosed at *C* the voltage across capacitor C_1 will again oscillate around zero. With C_1 short-circuited the steady-state voltage across C_2 is twice normal. Therefore, when the switch is closed, the voltage of C_2 is accelerated from -1 toward twice normal but will overshoot to +5 times normal. Now assume that the switch is again opened at *D*, at which time C_1 has a charge corresponding to a voltage of +3 and C_2 has a charge corresponding to a voltage of +5. These charges will equalize leaving a charge corresponding to a voltage of +4 on each capacitor. Adding normal capacitor voltages to the voltage due to residual charges gives a voltage of +3 across C_1 and of +5 across C_2. These are steady-state voltages, and as they are the same as the corresponding voltages, at the instant the switch is opened, there will be no transient. It should be noted that



(*a*) (*b*) (*c*)

Figure 6. Arcing ground (circuit 3a, theory III)

(*a*)—Voltage across capacitor C_1

(*b*)—Voltage across capacitor C_2

(*c*)—Switch voltage

the capacitor voltages build up according to the series 1, 3, 5, . . . which is the same as for the switching operation in figure 4*b*. Thus it can be seen that arcing grounds and switching operations may build up high voltages by the same mechanism of intermittent arcing.

In figure 4*d* theory II is illustrated for the circuit of figure 3*b*. This differs from figure 4*b* in that the switch is not opened at the first current zero which is controlled by the natural-frequency component, but it is opened at a later instant corresponding to the fundamental current zero. Theory I is illustrated in figure 4*f* for the circuit of figure 3*c*. In this case the point of switching is controlled entirely by the high frequency. It is assumed that the oscillation is of very high frequency and the fundamental does not change during the interval of time considered.

In the above cases no damping was considered. Damping may appreciably

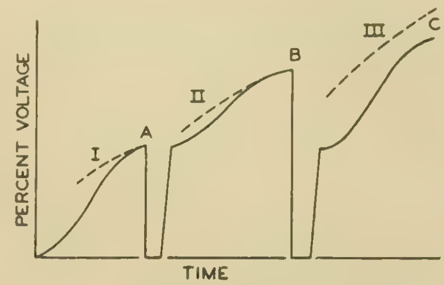


Figure 7. Dielectric recovery characteristics assumed for conditions of figure 5b

Note: Corresponds to the abstract circuit of figure 3*b*. All physical circuits have capacitances on source side which will introduce additional oscillations such as shown in figure 5*b*. Such demand different dielectric recovery characteristics to obtain the two restrikes shown

affect the shape of the curves and will definitely limit the maximum voltages obtainable.[8] The effect of damping can be seen by comparing figures 4*d* and 4*e*. In figure 4*e* the high-frequency component of the capacitor voltage is assumed to be damped out in one-half cycle of the fundamental. The voltage is limited to three times normal under the assumed conditions of damping. In practical circuits the damping usually will not be as large as considered in figure 4*e* but will be large enough to require consideration.

Another factor which cannot be neglected is the ratio of the natural frequency to the frequency of the generated voltage. In the above cases this ratio has been assumed to be very high. If this ratio is low the voltage will not build up as shown; the mechanism will be the same but the magnitudes will be less. For example, if in figure 4*b* the ratio were low the voltage would not build up to three times normal with one restrike because the fundamental decreases appreciably by the time the high-frequency component reaches its negative crest value. In many actual systems the natural frequency of the circuit may not be much above the supply frequency. For example, in a Petersen-coil grounded system the principal natural frequency is the same as the fundamental frequency.

At this point it is desirable to present some oscillograms of circuit transients in accordance with the foregoing discussion. While these oscillograms were actually taken on the a-c network calculator used

in the general studies discussed subsequently, it is sufficient for present purposes to consider them as transients obtained on simplified circuits of figure 3 subjected to switching transients in accordance with theories I, II, and III. Typical oscillograms are shown in figures 5 and 6. Figure 7 is a replot of the oscillogram of figure 5*b* with all voltages plotted on one side of the time axis. In this curve the high-frequency transients due to oscillations on the source side of the switch have been neglected. It will be noted from figure 7 that the first restrike takes place at point *A* at a value close to twice normal voltage. This requires that the dielectric strength of the arc path recovers along some curve such as *I*, that is, along a curve which is above the curve of recovery voltage until at point *A* where they intersect. During the time the arc path is conducting, the dielectric strength of the switch is practically zero. When the arc is again extinguished, the dielectric strength curve again starts from zero but recovers much more rapidly and intersects the curve of recovery voltage at the point *B* causing a second restrike. After the next arc extinction the dielectric strength curve must recover still more rapidly in order to meet the assumed condition that no restrike should occur at the point *C*. These curves show the requirement for the dielectric strength of the arc path to obtain high overvoltages. If curve *I* were not as high as shown, the restrike would have occurred at a lower voltage and the capacitor voltage would not have been as large as shown in figure 4*b*. If the dielectric strength had built up at a more rapid rate, no restrike would have taken place. It can definitely be concluded that the dielectric strength

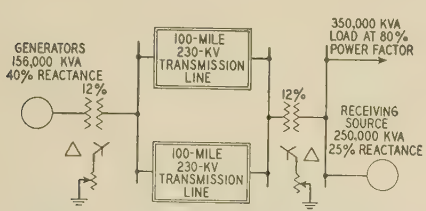


Figure 8. Schematic diagram of system selected for study

Transformer reactance: per cent on 156,000 kva

must build up at a higher rate after any extinction than it did after the preceding extinction in order to develop cumulatively higher voltages. This phenomenon is unlikely to take place in open air between stationary contacts because such an arc path is unlikely to develop the required dielectric recovery strength. In confined arcs, where the pressure may increase after each conduction period, this phenomenon may take place. Separation of breaker contacts will have a tendency to cause higher dielectric strength recovery rates after each conducting period because of the increasingly larger contact separation. These requirements of the arc path probably provide an explanation for the difficulties which have been experienced in attempts to produce high voltages by intermittent arc paths in air over insulator strings. In this connection it may be observed that the conditions for producing high voltages by intermittent arcing are somewhat more favorable for the case of the apparatus failure under oil than for the case of a flashover of an insulator string. The sequences may be for an apparatus failure under oil to cause a line flashover instead of for a line flashover to cause apparatus failure.

The foregoing discussion has been based on simple circuits for the purpose of illustrating the essential element of the theories of intermittent arcing. All actual systems are relatively quite complicated and cannot be reduced to the simple circuits used in the illustrations. Because of this complexity of actual systems it has been found that the maximum voltages with intermittent arcing are not obtained exactly in line with the preceding theories. More specifically, the maximum voltages are obtained for simple

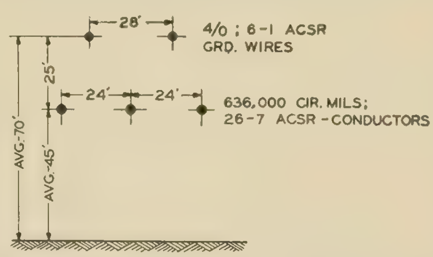


Figure 9. Configuration of transmission line

circuits with the restrikes occurring at either the high-frequency voltage crest or at the fundamental-frequency voltage crest. With complicated circuits it has been found that higher voltages can be obtained by modifying the point of restriking in one direction or the other from these points. This of course is due to the fact that the oscillating circuits have several natural frequencies. The determination of the exact manner of restriking is very difficult to define in analytical fashion. Because of this fact and because of the importance of damping it has been found to be impractical to study arcing grounds and switching transients by the usual methods of mathematical analysis. For this reason it has been found desirable to represent the systems in miniature on the a-c network calculator and to perform the actual switching operations by means of special commutators and this procedure is employed in the study given in part II.

Part II. A-C Network Calculator Study for a Range of Grounding Conditions on a Typical System

In order to study the magnitude and other characteristics of transient voltages produced by switching operations and faults with intermittent arcing, a typical transmission system was selected for a study on the a-c network calculator. Since these transient voltages are greatly influenced by the method of grounding, the neutral impedances of the system were varied through a wide range of resistance and reactance values, between the limits of the solidly grounded system and the ungrounded system.

The principal characteristics of the system selected for study are given in figure 8. This system consists in general of a hydroelectric generating station, the output of which is transmitted 100 miles over 230-kv transmission lines to a load, which is also supplied by local steam generation. The sending and receiving end transformers are considered to be star-connected on the 230-kv side in order to permit grounding, as discussed subsequently. The reactance characteristics of the different parts of the system are shown in figure 8, and the wire sizes and configuration of the transmission lines are shown in figure 9. It is assumed that the transmission lines are separated so there is no mutual effect between them. Also, the generators at both ends of the line are assumed to be in phase and to have the same internal voltage.

The general method used in setting up a problem on the a-c network calculator has been described in a previous paper.[9] In this method the selected system is set up in miniature on a three-phase basis and the circuit changes are accomplished by means of commutators. These commutators are designed to permit close adjustment of opening and closing a circuit or applying or removing a fault. The transient voltages are measured by a cathode-ray oscilloscope from which records on a film can be secured if desired. Because of the large number of circuit changes required for the representation of intermittent arcs to simulate arcing grounds and switching conditions on a system, it became necessary to provide a larger

number of commutators than used in previous studies.[9] Figure 10 shows this equipment together with the measuring and recording apparatus used in the present investigation.

The general method of setting up the network calculator, as previously discussed,[9,10] makes use of equivalent three-phase networks for each circuit element such as machines, transformers, and transmission lines. The character of these equivalent circuits is obvious and requires no comment except for the transmission lines and these are represented by the circuit shown schematically in figure 11.

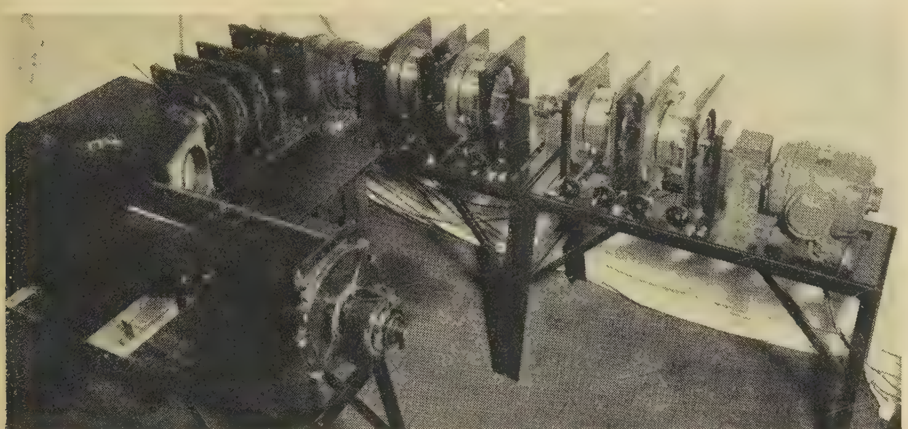


Figure 10. Switching and recording equipment

Throughout this investigation an attempt is made to obtain the highest voltages for a particular condition. As previously discussed, a slight deviation is made from theories I, II, and III, in order to obtain these voltages. This deviation affects the point at which restriking takes place and this is shifted from the fundamental-frequency voltage crest or the natural-frequency voltage crest to a point to give the maximum transient voltage. In the case of the arcing ground studies the fault is applied at the crest of the normal line-to-ground voltage and is then removed at the first current zero. The point of restriking is adjusted so as to give the maximum voltage for the number of restrikes considered. The fault is always removed at the first current zero following each restrike. In the case of switching operations the circuit is initially opened at a fundamental current zero. The point of restriking is adjusted so as to give the maximum voltage for a given number of restrikes. The subsequent circuit openings are always assumed to take place at the first current zero following the restrike.

In all cases the highest voltages at the point of circuit change are recorded. For example, in the arcing-ground case the voltages are measured at the receiver end. On the other hand, in the case of de-energizing an unfaulted line or the faulted line, the voltages are measured at the sending end, the point at which the switching is actually accomplished. When arcing grounds are considered on the system, several phase voltages as well as the neutral voltage are recorded. In the case of switching operations the voltages are recorded on the phase being switched, both on the line and supply sides as well as across the switch that is opening the circuit.

The voltages recorded are those that take place within $1^1/_2$ cycles from the first interruption considered. In some cases, either on account of system loss or on account of the relation of the natural frequency to the fundamental frequency, higher voltages may be experienced with one or no restrikes than with two or one restrikes, respectively. In some cases, particularly in the Petersen-coil case, the voltages after the $1^1/_2$ cycle period may continue to increase to a steady-state voltage of much higher value. In this connection it should be pointed out that with a Petersen-coil grounded system quite high voltages are obtained if the circuit is in tune at fundamental frequency and a residual voltage is produced as by some unbalance. For example, the opening of one phase of a system subjected to a three-phase or a line-to-line fault on the phase being opened will produce a steady-state voltage of many times normal.

In this investigation of transient overvoltages produced by switching operations and faults, four principal cases have been selected for study as follows:

1. Arcing-ground conditions on one phase to ground.

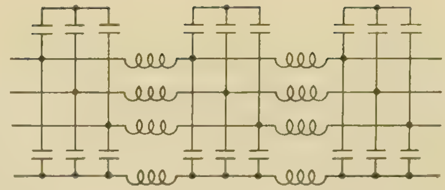


Figure 11. Equivalent network used for representing each 230-kv transmission line of figure 8

2. De-energizing an unfaulted line, one pole unit opening and the other two remaining closed.

3. De-energizing an unfaulted phase with a ground fault on one of the other phases, one pole opening and the other two remaining closed.

4. De-energizing an unfaulted phase with a ground fault on the two other phases, one pole opening and the other two remaining closed.

In general, arcing-ground conditions are for a fault on one phase. De-energizing of a line section is considered more important than energizing because for the latter the intermittent arcing is limited in duration by the closing of the switch. In the case of opening the faulted lines it is assumed that the unfaulted phase opens prior to the pole units of the faulted phase or phases. Such an assumption is based on the ability of the switch to recover dielectric strength at a high rate. This assumption tends to give the higher magnitudes of transient voltage. If the pole unit in the sound phase tends to open after the fault is cleared, then the voltages will be similar to de-energizing an unfaulted line. The voltages will range in values between these limits as the time of relative opening is varied. The conditions selected for study illustrate possible circuit-breaker operations on an actual system.

In this study the transient voltages are obtained for the conditions corresponding to both one and two restrikes. This number of restrikes may be taken as the equivalent of a larger number with the earlier restrikes taking place so quickly that they do not contribute much to the voltage magnitude.

One of the variable factors considered in this study of a typical system is the method of system grounding which includes both resistance and reactance values between the limits of a solidly grounded system and an ungrounded system. When the system is considered solidly grounded, the transformer at the sending end is solidly grounded when one line is considered in operation, and the transformers are solidly grounded at both ends when two lines are in operation. In the case of impedance grounding a reactor or resistor of varying ohmic value is con-

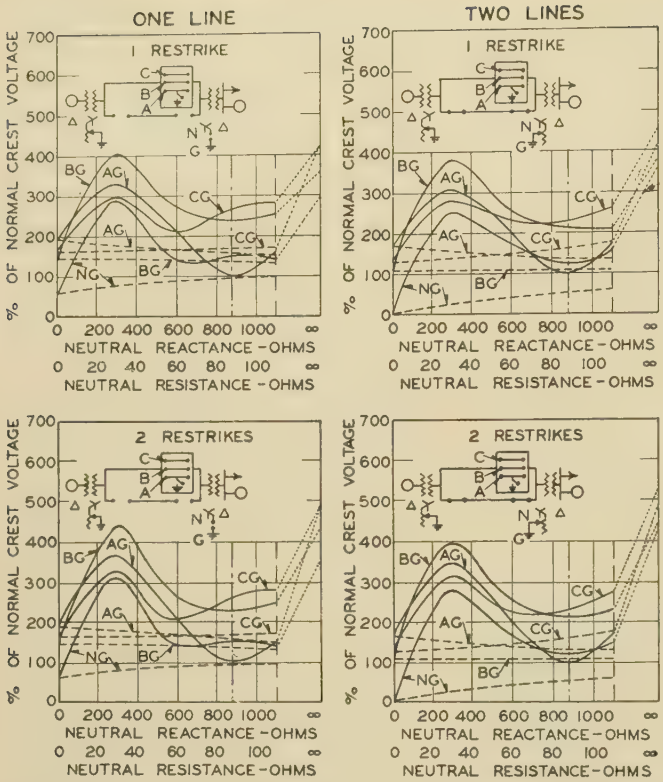


Figure 12. Effect of grounding impedance on transient voltages caused by arcing grounds

Solid curve: reactance grounding

Dotted curve: resistance grounding

Note: Letters on curves refer to lettered points on inset circuit

Petersen-coil reactance: 875 ohms

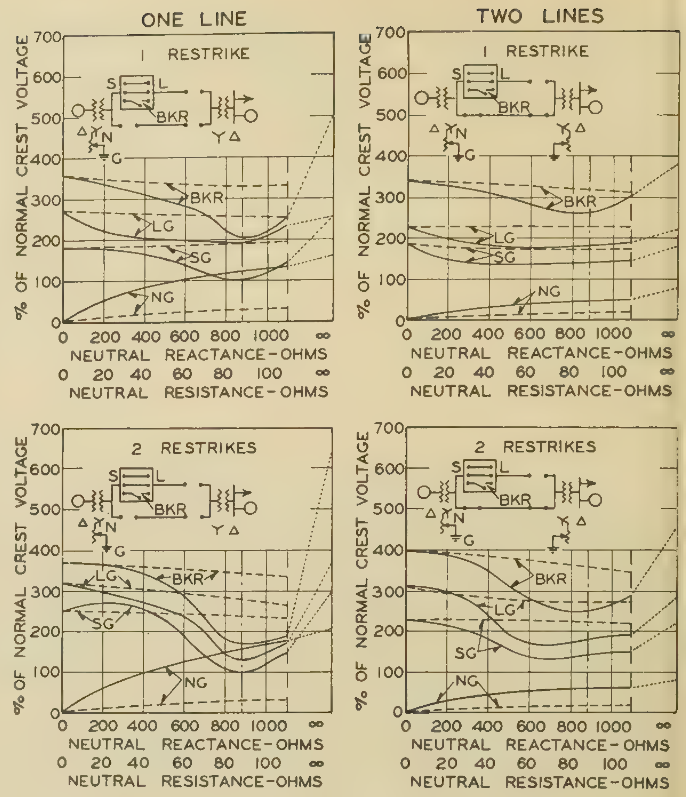


Figure 13. Effect of grounding impedance on transient voltages caused by de-energizing an unfaulted line

See subcaption of figure 12

sidered in the neutral-to-ground circuit at the sending end when one line is considered in operation, and a reactor or resistor of equal value is considered in the circuit in the sending and receiving ends when two lines are in operation. The ohmic values plotted on the figures to be discussed later are the actual values considered in the ground connection at one point. For example, 50 ohms on a system with one ground point is the value considered in the sending end ground. When two lines are considered in operation, 50 ohms corresponds to the ohmic value in the sending-end neutral connection and a like value in the receiving-end neutral connection.

The results of the a-c network calculator study are presented in graphical form in figures 12 to 15 inclusive. These figures give the transient voltages expressed in percentage of the normal line to ground voltage crest and are plotted as a function of the reactance or resistance in the neutral connection. The solid-line curves are for reactance grounding and the dotted-line curves are for resistance grounding. The value of neutral reactance which corresponds to Petersen-coil grounding is indicated. In each of these figures the data are plotted for one and two lines and one and two restrikes.

Discussion of the Results of A-C Network Calculator Studies

The limited range of previous investigations has made impossible a comparison with the present investigation except for the arcing-ground condition on the ungrounded system. For this case, results of the present study given in figure 12 show maximum voltages on sound phases of 5.2 times normal line to neutral voltage and 4.9 times normal as the corresponding value for the faulted phase. These figures are somewhat higher than the limiting values given by Peters and Slepian[7] for theory II and are comparable with their values for theory III. It is to be noted that the maximum values given in this study are based on two restrikes while the values given by Peters and Slepian are the maximum values for an unlimited number of restrikes. Peters and Slepian concluded from their investigation that the most probable mode for an arcing ground was in accordance with theory II. This conclusion is not contradicted, but the present investigation does establish the fact that higher voltages are possible with intermittent arcing in accordance with modifications of theories I and III.

Figure 12 brings out the fact that high transient voltages may be avoided by the use of the solidly grounded system or the Petersen-coil grounded system, both of which have been employed for many years to avoid the abnormal voltages encountered on ungrounded systems. The voltages corresponding to resistance grounding are of fairly uniform and relatively low value for the range of resistance studied. However, it is to be noted that for high values of neutral resistance approaching infinity, the transient voltage values will approach those of the ungrounded system. The study brings out in striking fashion the fact that there is a value of reactance intermediate between the solidly grounded system and the Petersen-coil grounded system for which the transient voltages are almost as high as with the ungrounded system. This value of neutral reactance is approximately one-third of the Petersen-coil value. A similar relation has been found in a study on a lower-voltage system.

Examination of figure 12 indicates that arc suppression with the Petersen coil may be obtained for some deviation from the tuned value, which is in accordance with operating experience. The voltages on the faulted phase, given in figure 12, for

Petersen-coil grounding, do not show a marked change in magnitude as the reactance is varied in proximity to the tuned value. It is of further interest that the magnitudes of transient voltages are higher for two restrikes than for one restrike, and that there is no appreciable difference between these voltages for one and two lines in service.

The transient voltages resulting from the de-energizing of an unfaulted line are shown in figure 13. The most striking feature of this figure is the fact that the lowest transient voltages, with the exception of those across the neutral impedances, are obtained with Petersen-coil grounding. In all cases the neutral-point voltage increases with increasing values of neutral impedances. For the range of practical values of neutral impedance, there is no appreciable difference between the voltages obtained for the case of one and of two lines. However, in the case of a free neutral system the voltages are appreciably lower for the larger amounts of connected line. Again in these studies it is to be noted that the magnitude of transient voltages increases for both one and two restrikes.

Figure 14 shows the transient voltages for the condition of de-energizing a line section with a fault on a phase other than that which is being switched. It is of interest to note that the voltages in all cases of reactance grounding increase from the solidly grounded case to the free neutral case. The voltages between neutral point and ground also increase for resistance grounding as the magnitude of the resistance is increased. It is to be noted that the voltages for the Petersen-coil grounded system are definitely higher than for any of the lower values of reactance grounding. This is to be contrasted with the dip in the voltage curves of figures 12 and 13. In figure 14 there is a definite increase in the voltages with two restrikes as compared to the case with one restrike. As would be expected, the greater the amount of line connected, the lower the magnitude of the transient voltages encountered.

Figure 15 shows the results of a study similar to that of figure 14 except that a double instead of a single line-to-ground fault is applied to the line section being de-energized. In general, the comments are the same as for the case of figure 14. For reactance grounding the transient voltages increase very rapidly for a relatively small addition of neutral reactance, so that for a very nominal amount of neutral reactance the transient voltages closely approach those of the free neutral system. In this case the voltages experienced with the Petersen-coil grounded system are practically the same as for the free neutral system.

It should be emphasized that the results obtained in the a-c network calculator studies are based on a definite number of restrikes which are spaced at such intervals as to give the maximum voltage for this number of restrikes. Thus, in the average case, since the restrikes may not

Figure 14. Effect of grounding impedance on transient voltages caused by de-energizing line with single line-to-ground fault

See subcaption of figure 12

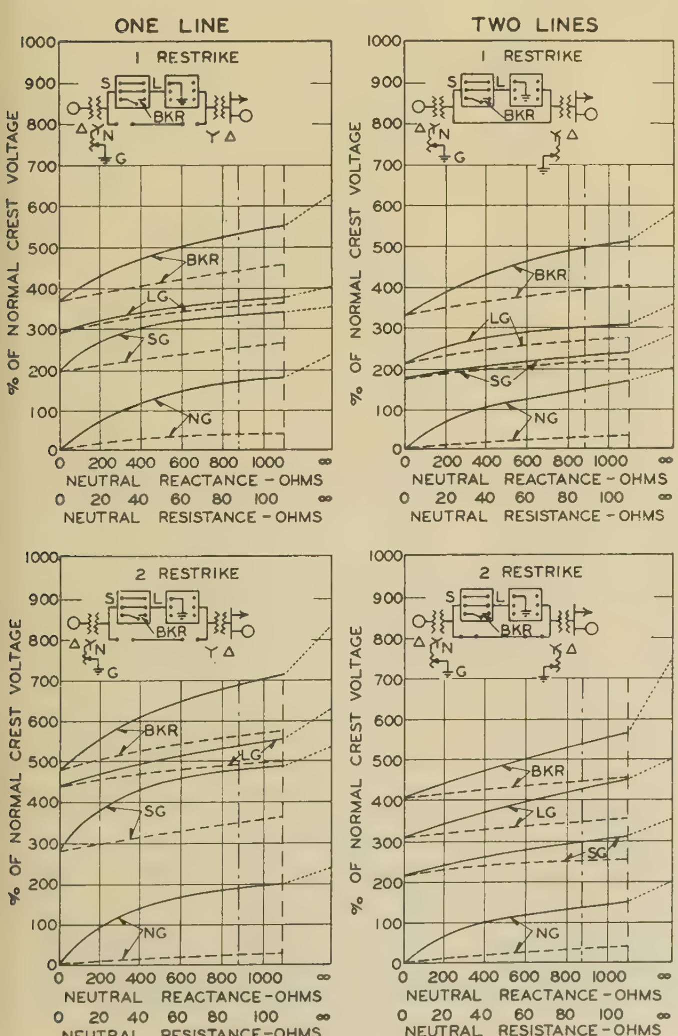
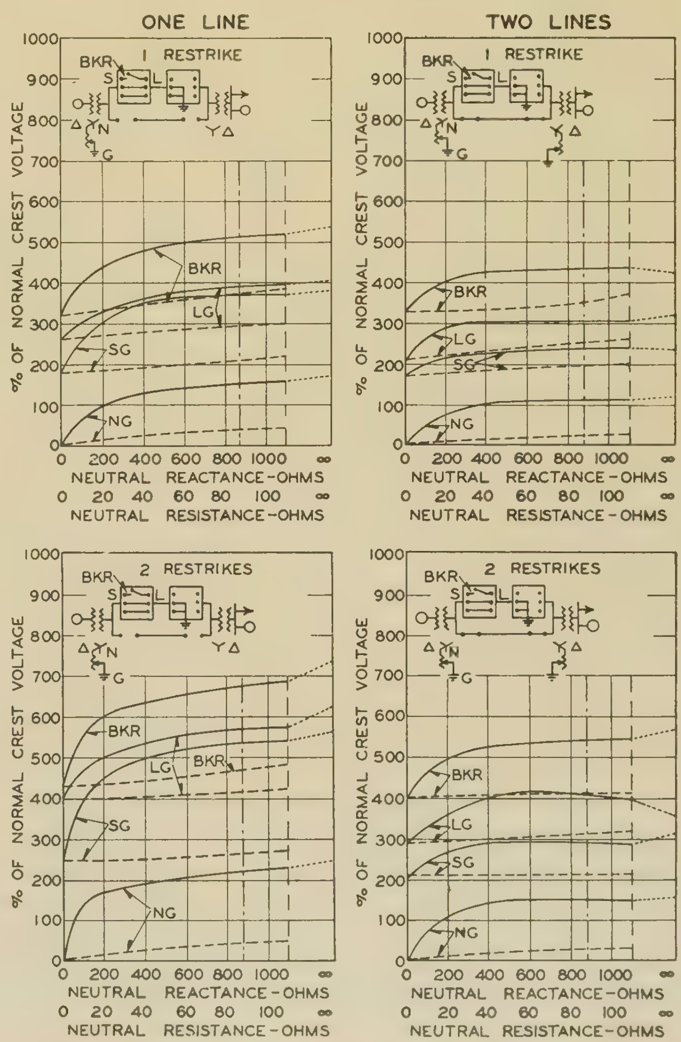


Figure 15. Effect of grounding impedance on transient voltages caused by de-energizing line with double line-to-ground fault

See subcaption of figure 12



occur at the optimum point, the voltages will be of lower magnitude giving a probability curve for the voltage. It is, of course, appreciated that only a minority of the cases of system faults and switching produce abnormal voltages.

The a-c network calculator studies have also been based on the assumption that the transient voltages of increasing magnitude may be impressed on the system without in any way changing the characteristics of the system. Actually, the transient voltages will be limited by other factors which become of increasing importance as the magnitude of transient voltage increases. On some systems the effect of corona will importantly limit the magnitude of transient voltage by introducing losses in the oscillating circuits. Under some conditions excess voltages will produce increases in exciting current particularly at the lower frequencies, but this factor is ordinarily unimportant. Transient voltages may also be limited by the operation of lightning arresters or protective gaps which are adjusted so as to operate below the flashover level of line or apparatus insulation. The presence of these devices may importantly limit the magnitude of transient voltages encountered on a particular system. Finally, the magnitude of transient voltage is limited by the flashover characteristics of line and apparatus insulation. Operating experience does support the results of this study in that some switching operations do result in flashover of line or neutral point insulation.

It is of interest to compare the results of this investigation with those obtained in the field. The maximum voltages of figures 12 to 15 correspond very well with the limiting voltage of six times normal indicated in figure 1. It is believed that the shape of the curves of figure 1 should not be accepted too freely as these are no doubt influenced by the flashover of lines or apparatus, or the operation of lightning arresters.

The study shows when Petersen-coil grounding is used that the voltages are a minimum for the cases of arcing grounds and switching on unfaulted lines, but that higher voltages may be experienced when a faulted line is switched. This coincides with the conclusion drawn some years ago by Oliver and Eberhardt,[11] "All switching, both hand and automatic, should be done with the (Petersen) coil out of service—namely, with the system neutral solidly grounded." Consideration is being given to relay schemes to accomplish automatically this result by short-circuiting the Petersen coil prior to the opening of a faulted circuit.

That more equipment failures have not occurred may be explained by the fact that the voltages have been limited by flashover at a weak point on the system. This might be at one of several pieces of equipment, or, in most cases, at a lightning arrester that operates. In cases where there is considerable energy in the oscillation, particularly on the larger systems, the phenomenon may even cause the failure of a lightning arrester.

If the maximum voltages appeared on apparatus, it might be severely stressed as the voltages are in excess of the 60-cycle tests applied to apparatus. The severity of this will depend on the breakdown characteristics of apparatus at higher frequencies, a point which needs further investigation.

The results of this study should be of assistance in selecting the voltage class of insulation for the transformer neutral and the Petersen coil. For all cases of switching, the neutral voltages for the ungrounded system are substantially the same as for the Petersen-coil grounded system. However, for the arcing-ground case, the neutral voltages are considerably higher with an ungrounded system than with a Petersen-coil grounded system, although these voltages are less than those experienced with an arcing ground on an ungrounded system.

Conclusions

1. The a-c network calculator has made practical the study of transients caused by switching and faults, including arcing grounds and other intermittent arcs, for a broad range of grounding conditions.

2. This investigation shows that

(*a*). Higher maximum transient voltages may be obtained by modifications of previously advanced theories of intermittent arcing grounds.

(*b*). Theories proposed for arcing grounds are applicable to switching with intermittent arcing.

3. The results of a study of the effect of grounding on a typical transmission system, subjected to different conditions of switching and faults with intermittent arcing, as presented in part II, show:

(*a*). The highest transient voltages are obtained with the ungrounded system.

(*b*). These voltages may largely be avoided by the use of the solidly grounded or Petersen-coil grounded systems.

(*c*). For one value of neutral reactance intermediate between the solidly grounded system and the Petersen-coil system, the transient voltages are about as high as with the ungrounded system.

(*d*). The transient voltages for arcing grounds and de-energizing unfaulted lines are lowest for the Petersen-coil system; however, unless the Petersen coil is short-circuited for the cases of de-energizing faulted lines their transient voltages are relatively high.

(*e*). In general, the lowest transient voltages are obtained with the solidly grounded system.

4. The method of investigation and the results of the study on a particular system are held to be pertinent to the problem of determining the voltage class of neutral point insulation on impedance-grounded systems.

5. The results presented in this study are believed to provide an explanation for some of the line and neutral point flashovers, multiphase faults, and arrester failures that have been experienced on actual systems.

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Discussion

D. C. Prince (General Electric Company, Philadelphia, Pa.): This paper represents a valuable addition to the literature on voltage recovery transients. The authors by calculating show the possibility of generation of overvoltages from four to eight times normal due to switching. The question naturally arises whether the assumptions giving rise to these high overvoltages

are valid. The figures given rest upon the premise that two restrikes will take place under the worst possible conditions. If no restrikes occurred, these overvoltages would not be generated. If, on the other hand, more restrikes occurred even higher overvoltages might occur. Citation of a few examples of restriking not accompanied by overvoltages hardly proves that such overvoltages would not occur were the assumptions of the paper not realized. The important conclusion to draw from the paper would therefore seem to be that circuit breakers should be selected which have a minimum tendency to restrike.

C. L. Gilkeson (Edison Electric Institute, New York, N. Y.): The use of the a-c network calculator for studies of transients, as developed by Evans and Monteith, has opened the way to an understanding of hitherto obscure phenomena. The present paper, dealing with the relation between transient overvoltage and neutral impedance, is most interesting. At the same time it emphasizes the need for more knowledge in other related fields in order that the significance of the paper may be properly evaluated.

In the discussion of figure 7, the authors have clearly stated the characteristics of the dielectric recovery in the arc, with respect to the recovery voltage, which permits the production of voltages of the type they are here discussing, namely, that the dielectric strength increases between successive restriking of the arc. It seems reasonable to find that such a condition does exist in a circuit breaker, where the length of the restriking path increases with time. Arcs across insulators would not be expected to have such characteristics. A question of importance is whether faults in confined space, such as cable failures, bushing failures, or arcs under oil, in expulsion tubes, or expulsion fuses may have such characteristics. If the condition in the arc, necessary to cause high transient voltages, can occur only during the opening of a breaker the problem takes on one aspect, since its duration is limited, however, if it may occur during an arc in a confined space the problem takes on an entirely different aspect.

In the study of overvoltages, conducted by the Joint Subcommittee on Development and Research of the Edison Electric Institute and Bell System, a number of records have been obtained for faults in confined spaces, such as cable failures, bushing failures, and arcs under oil. These records were obtained from operating systems and were for the most part from automatic magnetic oscillographs, supplemented in one case by an automatic cathode-ray oscillograph. The systems included an extensive 140-kv isolated neutral system, a 140-kv system equipped with Petersen coils, a 26-kv system with a 75-ohm neutral resistor, and a 44-kv system with a 150-ohm neutral resistor. For faults in confined space, many of the records showed that during a part of the disturbance, the arc was extinguished and restruck, every half cycle, every cycle, or at irregular intervals. In all cases the restrike appeared to be controlled by the fundamental-frequency voltage, occurring at or near the crest of the voltage wave. In no case did the restrike occur at a higher voltage than the normal crest value. In most cases the extinction of the arc appeared to take place at the current zero of fundamental frequency but in some cases at a current zero of higher frequency. An oscillogram illustrating the conditions for a fault in a confined space is given in a paper by Jeanne and Gilkeson, AIEE Transactions, volume 53, 1934.

There have been a few instances where intermittent extinction and restriking of an arc might have been associated with an arc in open air. However, the condition is so rare that it is probable that if all the facts were known it would have been found that these few cases were also associated with faults in confined space.

To summarize the results of these observations, it appears that intermittent arcing of the type discussed does not occur across insulators in open air but may occur for arcs in confined space. While the data do not indicate an increase in dielectric strength between successive arcs they do not preclude this possibility. A study of the dielectric recovery for arcs in confined space, such as cables or bushings or in expulsion tubes or fuses, appears to be warranted.

A point of considerable interest in the paper is the relation between overvoltage and neutral impedance. One important conclusion of the paper is that under certain conditions a Petersen-coil system, badly out of tune, might be subjected to very high transient overvoltage. However, for moderate values of neutral impedance, 100 ohms resistance (the maximum considered in this paper), or 100 ohms reactance, the curves of figures 12 to 15 indicate that the overvoltage is little different from the condition of no impedance in the neutral. It should be recognized that a 100-ohm neutral impedance, on the 230-kv system represented, is equivalent to a much lower impedance on a system operated at a lower voltage.

Figure 14 shows an overvoltage of about 4.4 times normal, for a solidly grounded neutral for two restrikes on a single line. This value of overvoltage is near the upper value of overvoltages recorded for switching surges, and helps to explain why there is no consistent relationship between the observed switching transients and the method of operating the system, whether with impedance in the neutral or solidly grounded.

P. A. Jeanne (Bell Telephone Laboratories, Inc., New York, N. Y.): Due to my contact with the study of problems involved in fault-current limitation, I have found the papers relating to transient overvoltage, presented at this session, most interesting. The authors deserve a great deal of credit for their contributions to a difficult subject. My comments, while presented specifically in connection with the Evans, Monteith, and Witzke paper, also refer in general to the other papers on this subject. Mr. Gilkeson in his discussion, has referred to the data on overvoltages obtained by the Joint Subcommittee on Development and Research of the Edison Electric Institute and Bell System which tend to substantiate the idea expressed in the Evans, Monteith, and Witzke paper that an arc in air is unlikely to go through the sequence of extinction and restriking required for the cumulative building up of overvoltage according to the arcing-ground theory. I want to add a few comments based on these same data. The oscillograms from the resistance grounded systems and isolated system, referred to by Mr. Gilkeson, confirmed the possibility of some restriking during the opening of breakers which clear a faulted line. For the systems studied, the restriking appeared to be confined to the faulty phase, and usually occurred before the recovery voltage had reached a value equivalent to the crest of the normal-frequency wave. The resulting overvoltage on the sound phases following the restriking did not exceed that to be expected from the simple initiating transient.

An examination of the oscillograms of breaker-cleared two-phase-to-ground faults obtained from the Petersen-coil system over a period of five years showed overvoltages of about the same magnitude as those from the one line-to-ground self-cleared faults, indicating that the clearing sequence requisite for producing the higher voltages is likely to be of very infrequent occurrence.

With regard to the Clarke, Crary, and Peterson paper, I should say that the oscillograph observations tend to substantiate the conclusion reached therein, namely, that the maximum voltage generally will be in the order of that determined by the simple initiating or clearing transient. This was the order of magnitude of the maximum voltage recorded by oscillographs capable of recording up to around 3,000 cycles. Surge recorders, where used, tended to indicate considerably higher maximum voltages than the oscillographs, except in one case where a capacitance-coupling potentiometer in place of the usual string-insulator potentiometer was used to connect the surge recorder to the line. Since many of the surge-recorder records include the effect of lightning, as well as the transients due to a fault itself, it has been found difficult to determine correctly the magnitude of the overvoltage due to the fault alone. By discounting portions of a surge-recorder figure which appeared to be due to lightning or similar steep-wave-front phenomena, it was found that the maximum voltages deduced from the surge recorders tended to be from 30 to 40 per cent higher than shown by the oscillographs. It seems difficult to reconcile these differences, particularly as in a recent installation a good check between a magnetic oscillograph, a cathode-ray oscillograph, and a surge recorder, was obtained on the system where the capacitance potentiometer was employed to couple the latter two types of instruments to the line.

These papers indicate a marked advance in the analytical and laboratory testing technique for determining overvoltages. On the basis of these studies it should be possible to choose a system or systems having the characteristics most likely to contribute to high overvoltage, install automatic cathode-ray oscillographs, and determine whether the high voltages are realized.

C. Concordia, E. M. Hunter, and **H. A. Peterson** (all of General Electric Company, Schenectady, N. Y.): During about the last 20 years, many papers discussing the effects of restriking of the arc in a fault or interrupting device have appeared. Some of these have presented test results, but most have proposed theories which

could be fully developed and applied only in an approximate form. In addition, the fundamental assumptions of these theories regarding system behavior were on a rather insecure basis. The present paper is particularly valuable since it corrects the first fault mentioned above and presents the results of a following out of a physically consistent restriking procedure, with damping taken properly into account.

The second item of whether or not the assumptions are sufficiently representative must be determined by comparison with operating experience and with test results. However, at least one of the assumptions (that, in case of clearing of faulted lines, one of the unfaulted phases clears first) appears very likely to be correct except in the case of single-line-to-ground faults on systems grounded through Petersen coils. In such systems, for line-to-ground faults, breaker action should not ordinarily be required at all because clearing should be accomplished by the Petersen coil. In case the fault is a solid one, and breaker action is required, the faulted phase might clear first. If the faulted phase does clear first in the particular system investigated, voltage will not be built up across the switch to any great degree for either resistance or reactance grounding, and voltages on the unfaulted phases will also be much lower than for interruption of the unfaulted phases. The reason for this is evident when the natural frequency or frequencies of oscillation of the system investigated are considered. For low natural frequencies it is not possible to build up high voltages due to restriking in the faulted phase because of the limited number of restriking-reclearing cycles that can take place before the fundamental component of voltage (which is the changing axis of oscillation of the natural-frequency components) passes through zero. In fact, if the natural frequencies are sufficiently low, the magnitude of the recovery voltage across the breaker following the first restrike and reclearing may be even lower than that following interruption at normal current zero.

The condition of restriking at the maximum point of each preceding recovery-voltage wave is of course seldom realized in practice. Instead, with certain types of interrupting devices, restriking might occur at lower voltages, but on the other hand, might continue for several cycles, with all phases restriking in case of interruption of line charging current. The magnitudes of voltage indicated by the curves of this paper are thus to be considered primarily as indications of the *relative* severity of different methods of grounding. Similar restriking studies which we have made on systems indicate that the voltages are about the same for either resistance or reactance grounding of the same ohmic magnitude, except for the dip in the curves with reactance grounding for certain cases.

It is important to bear in mind also that the results of this investigation apply only to a specific system. Similar switching procedure carried out on different systems has indicated that, in particular, neutral voltages are considerably affected by the amount of capacitance interrupted relative to the total system capacitance.

The authors conclude that all switching on Petersen-coil systems should be done with the Petersen coil short-circuited, and state that this conclusion is in agreement with that of J. M. Oliver and W. W. Eberhardt (authors' reference 11). It should be pointed out that the overvoltages discussed by Oliver and Eberhardt arose chiefly from series resonance. This source of overvoltage has been effectively reduced, in all subsequent Petersen-coil installations, by the use of iron cores instead of the air-core reactors used in Oliver and Eberhardt's application. Moreover, in the operating experiences to date on 22 applications of coils in the United States, extending over several years of operation, there has, to our knowledge, so far been no indications of dangerous overvoltages.

E. W. Knapp (The Shawinigan Water and Power Company, Montreal, Que., Canada): The papers which have been presented are of special interest to the power company operating long high-voltage transmission lines. It might be of interest to mention at this time a few instances of overvoltage during switching. The system under consideration consists of a double-circuit steel-tower line with metallic bussing at the receiving end only. The lines are 135 miles in length and operate at 165 kv. The insulation consists of 10 and 12 units; there are two overhead ground wires and some buried counterpoise. At the receiving end of the line there is an automatic oscillograph with a sensitivity of about 5,000 cycles. Voltage records are taken from line potential transformers and current records are taken from bushing-type current transformers on the line oil switches.

During the past few years there have been recorded a number of cases of momentary overvoltage. In some cases the sound line became involved at about the time that the initially faulted line cleared. In one case during 1938 during a line-to-ground fault on phase *C*, voltages of 425 kv and 660 kv crest were recorded on phases *B* and *A* to ground, respectively. In another case during a line-line-to-ground fault on *B* and *C* phases, successive increases of voltage occurred on phase *A* until a crest value of 720 kv was reached before either flashover or clearance. Both of these troubles were during lightning and both lines were eventually involved.

Perhaps the most interesting operation occurred due to a piece of wire creating a line-to-ground fault on phase *B* on one line about 18 miles from the receiving end. At the instant of clearing at the receiving end, a crest voltage of 320 kv was recorded on phase *C* which flashed over. The sending end of the line was cleared a few cycles later throwing 174,000 kw on to the sound line. The faulted line was then automatically returned to service with practically no loss of load and without additional flashover. There was no lightning during this trouble.

R. D. Evans, A. C. Monteith, and **R. L. Witzke**: Mr. Prince has commented on the assumptions on which our study of transient voltages due to switching operations were based. He has indicated that if there are more than two restrikes higher transient voltages than those given in the paper might be encountered. The magnitude of transient voltages to be expected is not to be increased in direct proportion to the number of restrikes that take place. While several restrikes may take place, the effect on the transient voltages as indicated by klydonograph studies will not exceed that of two restrikes occurring at such intervals as to produce maximum cumulative action. This is due to several factors including the probable sequence of opening of the pole units in an actual breaker, as pointed out in the paper. The spacing between restrikes is fully as important as the number of restrikes and it is misleading to consider the total number of restrikes alone since without proper spacing the action will not become cumulative.

Mr. Prince has also raised the question as to the characteristics of breakers that are desirable from the standpoint of minimizing transient overvoltages. If restriking is wholly avoided, cumulative action is prevented. However, it is undesirable for a breaker to have such characteristics as to force current zero or such as to operate with a high extinction voltage. High dielectric recovery strength, low extinction voltage, and nonforcing of current zero are, to an extent, conflicting considering the full operating range of a breaker. In the present state of the art the emphasis, in our opinion, should be placed on the circuit condition. The principal significance of our paper has to do with the selection of the circuit condition for minimizing transient voltage, as for example, by the use of a solidly-grounded instead of a reactance-grounded system, etc.

Gilkeson and Jeanne have reviewed the work on transient overvoltages which has been done by the Joint Subcommittee on Development and Research of the Edison Electric Institute and Bell Telephone System. This work has been of great value and confirms the thought expressed in our paper that arcs over insulators in air rarely produce transient overvoltages, but that the conditions of arcing in a confined space, as for example, in the failure of bushings or cable insulation, are more favorable for cumulative action by intermittent arcs.

Mr. Gilkeson has commented on the fact that the records obtained on cable and bushing failure showed intermittent arcing with restrikes occurring every half cycle, every cycle, or at intervals, but that these do not show that restriking has taken place at greater than normal voltage. However, it is to be noted that the dielectric recovery characteristic of the arc path varies from a value such that breakdown occurs at normal voltage or less when the arc restrikes every half cycle, to a value which is nearly twice normal voltage for the condition in which the arc is interrupted at a current zero and restrike does not take place within a half cycle. Thus the dielectric recovery strength varies from a value somewhat less to a value somewhat more than that necessary to produce cumulative action. This would mean that there should be a tendency under some conditions to start cumulative action. Mr. Gilkeson has pointed out that evidence of this action has not been obtained although the data does not "preclude the possibility of such action."

Mr. Gilkeson has also pointed out that the condition for cumulative action is of very short duration if it arises from a switching operation but may be of long duration if it arises from an intermittent arc or incipient

Influence of Resistance on Switching Transients

R. C. VAN SICKLE
MEMBER AIEE

THE voltage recovery transient appearing across the terminals of a circuit-interrupting device has been the subject of numerous studies during the past ten years. Their importance has justified these studies because they determine in part the severity of the duty impressed upon circuit-interrupting devices. These studies have consisted of mathematical analyses of circuit conditions, calculating board investigations and studies of oscillographic records of the actual transients, as they are obtained on systems and in high-power laboratories.[1]

The damping of the voltage recovery transient, is a function of the resistance of the circuit. It depends not upon the absolute value of the resistance, but upon the value of the resistance relative to the inductances and capacitances. Important variations in the transients can be produced by changing the values of the resistances.

For the consideration of complicated systems, use may be made of the a-c network calculator as discussed in another paper, "Power-System Transients Caused by Switching and Faults" by Messrs. Evans, Monteith, and Witzke (AIEE TRANSACTIONS, volume 58, 1939, pages 386–97). The calculating board can be set up to represent the capacitance and inductance relations and if these are chosen with proper respect to the resistances of the circuit, an approximation of the damping will be obtained.

In mathematical studies, the inductances and capacitances of the various parts of the circuit are determined or estimated and from them the voltage recovery transients are calculated. Except in cases more simple than are usually encountered in system studies, the resistances of the circuits cannot be included because of the added complication of the mathematical expressions. Consequently, the rate of damping of the transient is assumed on the basis of experience and a general knowledge of the effect of resistance is essential in these studies.

The damping of voltage recovery transients can be studied in relatively simple circuits which contain resistance, inductance, and capacitance. These transients can be calculated and the rate of damping determined. The mathematical expressions for these circuits are relatively complicated but a graphical representation of the transients can be used to demonstrate the relations.

This paper presents graphically the effects of resistance on the closing and opening transients of inductive and capacitive a-c circuits. Typical simple circuits are assumed and the transients corresponding to various values of resistance are shown as curves. They demonstrate clearly the effect of resistance on switching transients.

The transients are described for single-phase circuits having a voltage source uninfluenced by the transients.

Paper number 39-51, recommended by the AIEE committees on power transmission and distribution, and protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 9, 1938; made available for preprinting January 3, 1939.

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1. See bibliography at end of paper.

Closing Transients of Inductive Circuits

When an a-c circuit containing resistance and inductive reactance is closed by a switch or when the resistance or

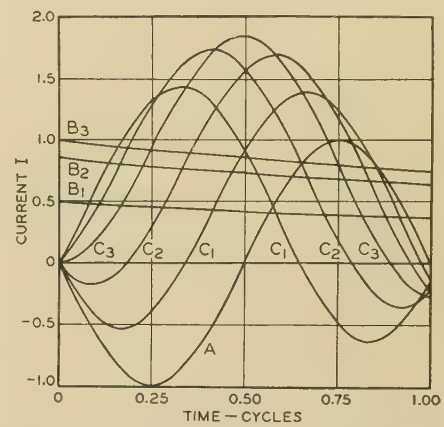

Figure 1. Effect of time of closing on asymmetry of current in an inductive circuit

reactance is altered, the steady-state condition of the system is changed and usually a current transient exists before the new steady-state is established. This is caused by the difference in magnitude of the instantaneous values of the steady-state currents before and after the instant the switch is closed. Since the current in the circuit cannot change instantly to the new value, a transient current component of amplitude equal to the difference is superimposed on the new steady-state current and decays as an exponential function of time. Such transient currents are shown in figure 1 and figure 2 and can be expressed by equation 1 given in the appendix.

If a circuit is closed at a time corresponding to a normal current zero, current is established without any transient as shown in figure 1 by the curve marked A. This current is symmetrical and contains no transient component. (Transients in the symmetrical component of current due to demagnetization of the generators are not considered in this paper.)

When the contacts close at a time other than a normal current zero, a transient component of current flows

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fault in a cable or other confined space.

Concerning the discussion by Concordia, Hunter, and Peterson, it is to be pointed out that from the standpoint of overvoltages produced by switching on the Petersen-coil-equipped system as discussed in the paper, there would be no appreciable difference caused by the substitution of an iron-core reactor for an air-core reactor. This is due to the fact that the magnitude of overvoltage produced by switching with intermittent arcing is not critical with respect to the tuned value of Petersen-coil reactance.

This method of producing overvoltages with a Petersen-coil system is not to be confused with that involving resonance. The latter effect was encountered in the investigations reported in the paper for certain fault and unbalanced circuit conditions such as produced by the opening of a breaker pole. In a symmetrical system high voltages should not be produced by series resonant action except as a result of some switching operation. Since switching operations may in themselves produce high transient voltages as a result of intermittent arcing it is not necessary, as Concordia, Hunter, and Peterson have done, to postulate both a switching operation and a series resonance for the unbalanced condition to account for high transient voltages.

E. E. Knapp has cited a case in which transient voltages greater than those corresponding to a simple break have been experienced. This supports the assumptions made in the study reported in our paper.

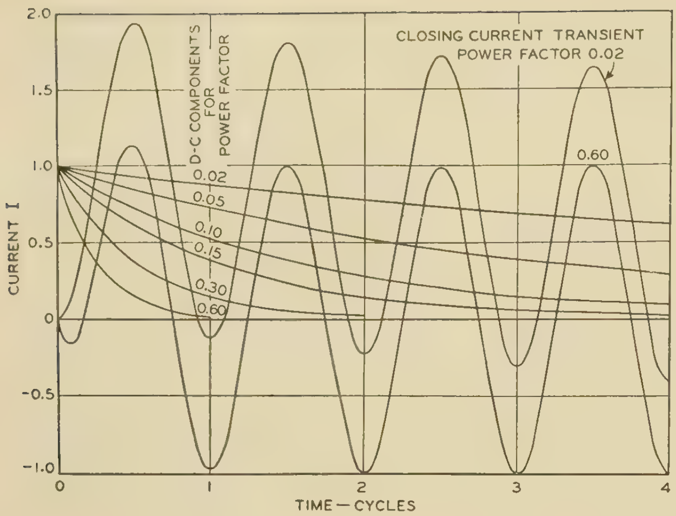


Figure 2. Effect of power factor on d-c component of closing current transient

and decays exponentially. These transient components are shown by the curves marked B in figure 1. The resulting current, which consists of the normal symmetrical current plus the transient component, is shown for different degrees of asymmetry by the curves C in figure 1.

The decay of the transient component of the asymmetrical current depends upon the value of the inductance and the resistance of the circuit. These same two elements, the resistance and inductance, determine the power factor of the circuit. Consequently the decay of the d-c component varies with the power factor of the circuit. The higher the resistance relative to the inductance; the higher the power factor of the circuit and the more rapidly the transient component decreases. This is shown in figure 2. The d-c components for fully asymmetrical waves are plotted for circuits having power factors from 0.02 to 0.60. The total currents including both the a-c components and the d-c components are plotted for two power factors. These curves show that in circuits having a low power factor, the asymmetry of the current will be maintained for a relatively long time. This becomes important in some current transformer and relay applications, because of possible saturation of the magnetic circuit. At the other extreme, the transients decay very rapidly; at 60 per cent power factor, the transients disappear at the end of one cycle.

These relations are important in the application and testing of circuit breakers. A high-speed circuit breaker operating on a low-power-factor circuit will interrupt the circuit before the d-c component has disappeared. A circuit breaker operating on a higher-power-factor circuit, opening after several cycles due to a relay timing or slower mechanical action will open only symmetrical currents.

The international rules for high-voltage circuit-breaker testing are based largely on the performance of circuit breakers that do not open until the d-c component has disappeared. Consequently, for 80 per cent of the tests demonstrating rupturing capacity they specify that the current shall have a direct component not greater than 20 per cent of the crest value of the alternating component. On test circuits having very low power factors, this value is not obtained until 10 to 14 cycles after the beginning of the short circuit. The curves in figure 2 show that by inserting additional resistance in the test circuit to raise the power factor to the maximum permitted by the rules (0.15) the time required to reach 20 per cent asymmetry can be reduced to only 1.6 cycles. A brief duration of the short circuit during testing is desirable because it minimizes the demagnetizing effect and results in less decay of the alternating component of current and in a higher restored voltage.

Testing under American rules is conducted with both symmetrical and asymmetrical currents up to the breaker rating or over the range of power available. Consequently in American laboratories, low circuit resistance is desirable to give a slowly decreasing d-c component.

Closing Transients on Capacitive Circuits

When a capacitive circuit is energized by the closing of a circuit breaker, a transient occurs which can cause an overvoltage on the system. The capacitance, if composed of relatively short cables or lines, so that the inductance is negligible, can be assumed to be lumped. The circuit which energizes it will have inductance, resistance, and capacitance. In the circuit shown in figure 3*a* it is assumed that the capacitance, C, to be energized is lumped and the capacitance, C_1 of the rest of the circuit is negligible.

When the arc strikes, the voltage at the switch will become ground potential and a voltage drop will appear across the inductance because the capacitance C is assumed to be uncharged and at ground potential. The inductance and capacitance of the circuit, connected by the striking of the arc, produce an oscillation with a possible crest value equal to twice the normal crest voltage. The resistance damps this circuit but its effectiveness depends upon its magnitude relative to L/C. The voltage recovery

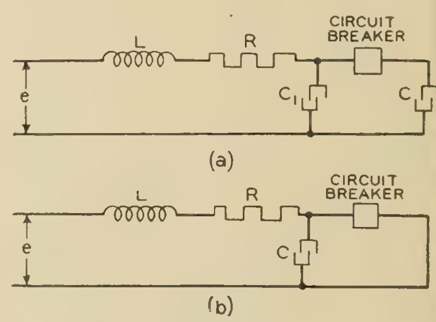


Figure 3

(*a*)—Energizing a capacitive circuit
(*b*)—Opening a simple inductive circuit

transient for several values of resistance are plotted in figure 4. The smallest value of R is comparable with those obtained in high-power laboratories. For resistance values less than the critical resistance, $R = 2\sqrt{L/C}$, the voltage transient is defined by equations of damped oscillations. For resistance values greater than the critical value, the voltage transients are defined by exponential equations and curves. The equations of these transients are numbers 2, 3, and 4 of the appendix.

In the closing of a high-voltage circuit by a conventional circuit breaker, the arc will strike before the contacts touch and the arc resistance will exert some damping effect on the transient. However, even with the maximum crest occurring with minimum damping, no dangerous voltage would be impressed on the system by the closing transient of this type.

In the curves of figure 4 it has been assumed that C_1 was negligible with respect to C. This causes the voltage transient to start at zero because the ca-

pacitance C is uncharged at the beginning of the transient. If C_1 is large enough to raise the potential of the capacitance C when the two are connected together by the closing of the breaker, the transient will start not from zero but from the potential taken by the capacitances. The amplitude of the transient is no longer the system potential at the time the arc strikes, E_m, but is the difference between E_m and the potential of the capacitors. The capacitance used in the formula for determining the transient is, of course, the sum of the capacitances C and C_1.

During the closing of a high-voltage breaker, it is possible for the arc to be extinguished and to restrike before the breaker is completely closed. This phenomenon may produce higher voltages because the line may be charged to a high potential by the first transient and

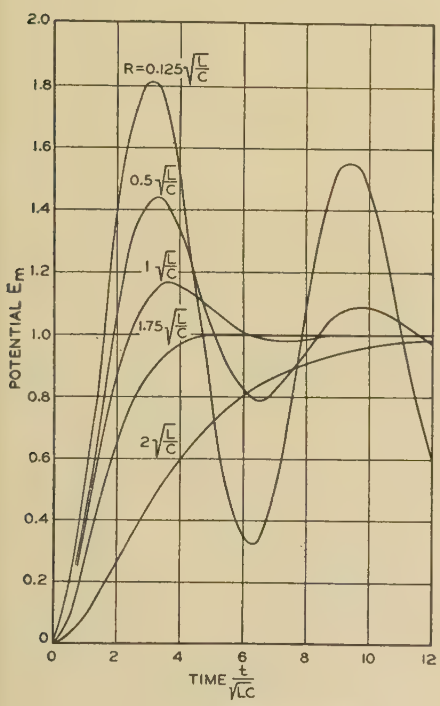


Figure 4. Voltage transients

Closing of capacitive circuit
Opening of inductive circuit

the amplitude of the second transient be increased thereby. However, the conditions on closing are not favorable for the building up of high overvoltages since the gap between contacts is becoming smaller. The phenomena is the same during the interruption of charging currents and more favorable conditions exist when the contacts are separating. Therefore this phenomenon will be described in connection with the interruption of charging currents.

When the capacitance is distributed over a long line it cannot be represented as a single lumped capacitance and more complicated circuits must be used. The principles are the same but the possible combinations are too numerous to consider here.

Opening Transients of Inductive Circuits

The opening of a short circuit or the opening of the circuit energizing an unloaded transformer may be considered as the opening of an inductive circuit. These currents lag the applied voltage by relatively large angles and consequently the voltage appearing across the contacts after the interruption is approximately the crest value of the applied voltage. The small capacitances of the circuit, which play no part in determining the current, are important in determining the voltage recovery transient. Likewise, the resistance of the circuit, which was negligible while the current was flowing, damps the voltage recovery transient. The resistance may be a small parallel load which is not removed by the opening of the circuit breaker or may be due to the resistivity of the conductors forming the circuit.

Series Resistance

Figure 3*b* represents a simple series circuit involving inductance, capacitance, and resistance. Frequently, this circuit is a part of a more complicated circuit and can be considered as a unit in itself independent of the rest of the circuit. The opening of this circuit is mathematically the same as the closing of a capacitive circuit, figure 3*a* and equations 2, 3, and 4. It has voltage recovery transients which are shown in figure 4. When the curves are applied to voltage recovery transients following the opening of a reactive circuit, the time scale will generally represent a shorter interval because the capacitance between the breaker and the inductance usually will be much smaller than the line or load capacitance.

For high-power laboratory circuits having relatively low resistance with respect to the inductance, the damping of the oscillation corresponds roughly with the curve for $R = 0.125\sqrt{L/C}$. Similar system circuits could be expected to have similar transients.

Combination of Series and Shunt Resistance

The opening of a circuit by a switch may not completely unload the source, or a circuit breaker may open in two steps, the first one inserting a resistance for limiting the current. Circuits of these types are represented by the diagram in figure 5.

This circuit too may be oscillatory or nonoscillatory depending upon the circuit constants. The critical condition is

$$\frac{R}{L} - \frac{1}{R_1 C} = \frac{2}{\sqrt{LC}}$$

The transients are similar to those shown in figure 4 for the simple series circuit.

The equations applying to this circuit are numbers 5, 6, and 7 in the appendix. The curves indicate and the equations show that the voltage transients occurring during the opening of an inductive circuit cannot attain potentials above two times the normal crest value of voltage.

General

The effects of an arc voltage prior to the last current zero and of the extinction of an arc before the normal current zero have been neglected but are important in reactive circuits. Both of these phenomena increase the amplitude of the transient and tend to raise the potential at the crest. Therefore, the maximum voltage which can be reached depends upon the characteristics of the circuit breaker and upon the circuit. A review of a large number of oscillograms of single-phase interrupting tests on high-voltage circuits showed no voltage in excess of 2.75 times the crest value of line-to-ground voltage and only a few reached 2.25 times.

Opening Transients of Capacitive Circuits

The interruption of a capacitive circuit would take place without a transient if the circuit had no inductance and no re-

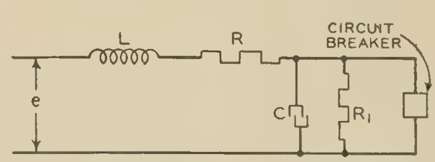


Figure 5. Opening a simple inductive circuit having both series and shunt resistance

sistance, if the arc was extinguished at current zero, and if the dielectric strength of the point of interruption increased to a little more than twice the crest of the applied voltage in a half cycle of the system frequency. A negligibly small transient occurs if the dielectric-strength

condition is fulfilled. On high-voltage circuits, such high dielectric strength is very difficult to obtain at the first current zero so the arc usually restrikes. The restriking initiates a transient which may produce high overvoltages. The circuit and the transients following a restrike are shown in figure 6.

Normally, the resistance is relatively low and the transients, both current and voltage, are oscillatory. On the first restrike, the current will pass through zero at times other than the normal current zero and if extinguished, may leave the capacitance charged to a voltage either above or below the applied voltage. With no resistance in the circuit and the worst possible rate of increase of dielectric strength, the theoretical rate of increase in voltage at successive restrikes would give 1, 3, 5, etc., times the normal crest of system voltage as shown in figure 7. However, this required that the gap after the first interruption (*a*) holds while the voltage across it increases from zero up to twice the normal crest (*a* to *b*), that it breaks down at this value and after a half-cycle of the transient interrupts again (*c*). If the breaker opening leaves the line charged as at *c*, the potential on the source side of the breaker will tend to return to the value of the applied voltage. It will do this through a transient similar to that which occurs on the opening of an inductive circuit, namely a damped oscillation produced by the inductance of the supply circuit and the capacitance still connected to it. This transient is damped by the resistance of the circuit but will reach a value approximately as shown at *d* and impress nearly four times normal crest voltage on the gap. As this transient dies (*d* to *f*) the voltage returns to approximately two times the crest value and increases due to change in the applied voltage from approximately two times to four times the crest value. This is reached at point *g*. To continue the building up of voltage, the arc must now restrike at *g* with little more voltage on it than it had at the point *d*.

Obviously this dielectric performance is almost, if not quite, impossible. Oscillographic data of actual interruptions by circuit breakers show a much more gradual build-up of voltage. In some cases, the breakdowns occur before the crest value of voltage is reached so that the amplitudes of the intermediate transients (*b* to *c*) are reduced. In other cases, the transient current may not be extinguished at its first current zero. If the current flows for two half-cycles of the high-frequency transient, the voltage on the line is reduced to a very low value. The damping of the transient by the resistance of the arc and of the metal conductors in the circuit also tends to reduce the rate of voltage build-up.

The influence of these modifying factors is shown by the oscillograms reproduced in figure 8. These oscillograms were taken during the interruption of capacitive currents in a high-power laboratory. In an oscillogram the line *A* represents the voltage across the terminals of the breaker, the line *B* the current flowing in the circuit, the line *C* the trip-coil current, and the line *D* the voltage on the source side of the breaker. Due to the circuit used, the oscillations in the voltage *D* are a maximum. The large capacitance representing the line was disconnected from the low capacitance of the source. Consequently, on a restrike, the amplitudes are not reduced by the equalization of potentials between them.

Oscillogram *a* shows the interruption of the charging current without any restriking. The voltage appearing across the breaker contacts was approximately two times the crest value of the applied voltage but the arc space did not break down.

Oscillogram *b* shows one restrike after a current pause during which the voltage rose to approximately 25 per cent of the crest value. The restrike was extinguished after a half-cycle of the high-frequency current. The arc did not restrike subsequently although the value of voltage appearing across the breaker contacts was approximately 1.7 times the crest value of the applied voltage.

Oscillogram *c* shows a restrike at a voltage approximating 50 per cent of the crest value of the applied voltage and the extinction after a half-cycle of the high-frequency current. This extinction occurred at approximately a normal voltage zero so that the subsequent voltage applied across the terminals of the breaker did not exceed the normal 60-cycle voltage. The arc did not again restrike.

Oscillogram *d* shows a restrike after approximately one half-cycle of restored voltage during which the potential applied across the breaker increased to approximately two times the crest value of the system voltage. The high-frequency current flowed for three half-cycles before it was extinguished. This left the line with very little charge as indicated by the almost symmetrical voltage wave appearing across the breaker contacts subsequent to the final interruption.

Oscillogram *e* shows the interruption of the charging current followed by a restrike at very nearly two times the normal crest of voltage applied across the breaker. Several cycles of high-frequency current flowed before the breaker interrupted and left the line charged approximately at the crest value of voltage. After a half-cycle of current interruption, the voltage across the breaker had again increased to two times the crest value of the applied voltage, and another restrike occurred. After about seven half-cycles of the high-frequency current, the arc was extinguished, the line being left charged at a potential about half the crest value of the system voltage. The maximum voltage appearing across the breaker subsequent to the final interruption was approximately $1^1/_2$ times the crest value of the system voltage.

Oscillogram *f* shows the best example of building up of voltage across the terminals of the breaker. Three restrikes occurred and on the last interruption, the line was left charged to about 1.85 times the normal line-to-ground voltage and during the subsequent half-cycle of restored voltage, the potential difference across the breaker increased to about 2.85 times the normal line-to-ground voltage.

On a 220-kv system where it was suspected that the interruption of line charging current was producing excessive

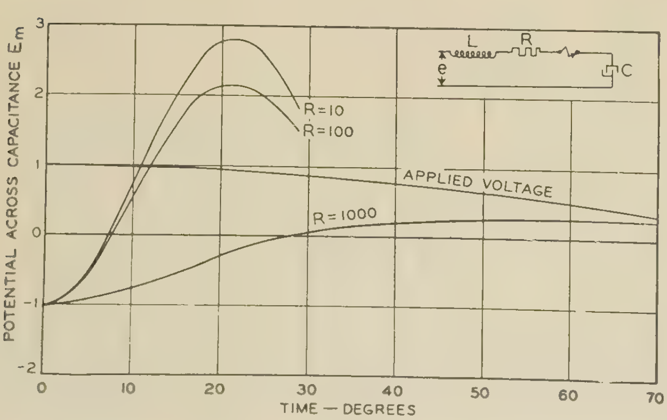


Figure 6. Restriking at crest of voltage wave with capacitance charged to potential of the previous crest

Curves show the effect of resistance on the transient voltage across the capacitance

$L = 0.1$ henry

$C = 10^{-6}$ farads

overvoltages, tests were made recently with a cathode-ray oscillograph recording the line voltage. Sections of lines up to 246.4 miles were disconnected and the maximum voltage recorded was 2.43 times the crest value of the system line-to-ground voltage. These voltages are harmless and indicate that on a typical system the high overvoltages obtained theoretically by neglecting the dielectric characteristics of the breaker, are seldom encountered.

Resistors in Circuit Breakers

Because of the effects of resistance on switching transients, circuit breakers using resistors have been built for many years. In Europe, prior to 1930, oil circuit breakers either plain-break or explosion-pot types sometimes had auxiliary contacts which inserted a resistance between terminals. The auxiliary contacts closed before and opened after the main or arcing contacts. Due to the relatively short gap between the arcing contacts when the auxiliary contacts were just touching, the probability of the resistance functioning to facilitate opening of the circuit was remote and their main service was in reducing the severity of the transients which occur on closing capacitive and transformer circuits. These resistances damped the inrush current to lines and limited the inrush current when a transformer was energized. The advantages gained hardly justified the additional complication and they disappeared from the market.

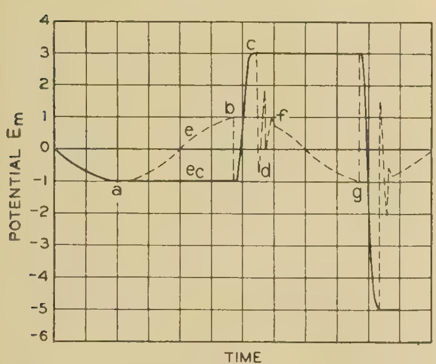


Figure 7. Theoretical transient voltages appearing at the two terminals of a circuit breaker during the interruption of a capacitive current

e = Voltage at the terminal connected to the source

e_c = Voltage at the terminal connected to the capacitance being disconnected

Note the transient *c-d-f* following a restrike which impresses almost as much voltage across the circuit breaker as the normal cyclic voltage change *f* to *g*

Some modern interrupters are being built with resistors which aid not only in closing circuits but also in the extinction of arcs. They facilitate the interruption of both charging current and reactive current. The resistors damp the transients which occur during the interruption of leading currents and reduce the overvoltages produced. They are generally inserted by being connected across either a part of the gap formed by the arcing contacts or a separate gap. The insertion of the resistor increases the power factor of the circuit, shifts the current zero to a point in the cycle where the applied voltage is less, and reduces the overvoltages which can occur on the system.

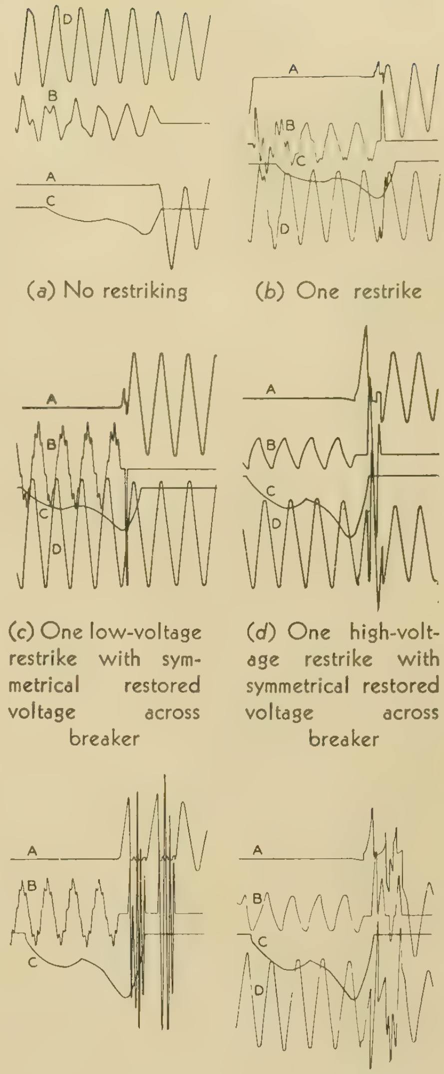
In lower-voltage circuits where heavy currents are the problem, resistors are used to facilitate the interruption of short-circuit currents and thereby increase the interrupting ability of the circuit breakers. An arc inserts the resistance which reduces the current, increases its power factor, and thus brings the short circuit within the limits which can be interrupted directly by the arc extinguishing means provided for the complete opening of the circuit.

The value of the resistance used depends upon the rated voltage and the rated rupturing capacity of the breaker. The magnitude of the voltage appearing across the gap which inserts the resistance depends upon the system voltage, the reactance of the circuit, and the resistance. The relations are given in the appendix as equation 8. These relations are demonstrated by figure 9 with voltage plotted for values of r/x of 0.1, 1, and 10. With $r/x = 0.1$ the voltage appears across the resistance and arc gaps at a very low rate and reaches only a low maximum value. The arc could be easily extinguished but the resistance makes only $^1/_2$ per cent difference in the total impedance of the circuit and consequently does not aid to any measurable extent in the ultimate interruption of the circuit.

With $r/x = 1$ the voltage appears more rapidly and reaches a value of about 50 per cent of the applied crest on the first peak. The second peak, a half-cycle later and consequently less important, reaches 70 per cent.

With $r/x = 10$ the transient component decreases very rapidly and disappears in about 20 degrees. The voltage rises rapidly across the resistance and inserting gap but does not exceed the crest value of applied voltage. The impedance of the circuit would be increased about ten times and consequently the current to be interrupted by the second gap is reduced to about ten per cent of the full short-circuit current.

Still further reduction in the current can be obtained by higher values of r/x. However, the effect of capacitance across the gap becomes important when the current flowing through the resistor becomes comparable with the current required to charge the capacitance.



(*a*) No restriking

(*b*) One restrike

(*c*) One low-voltage restrike with symmetrical restored voltage across breaker

(*d*) One high-voltage restrike with symmetrical restored voltage across breaker

(*e*) Two high-voltage restrikes

(*f*) An example of building up voltage across breaker

Figure 8. Typical oscillograms of the interruption of charging current at about 22 kv in a high-power laboratory

A—Voltage across breaker
B—Current
C—Trip-coil current
D—Voltage line-to-ground on supply side

Note that none of the oscillograms show the theoretical rate of voltage increase given in figure 7

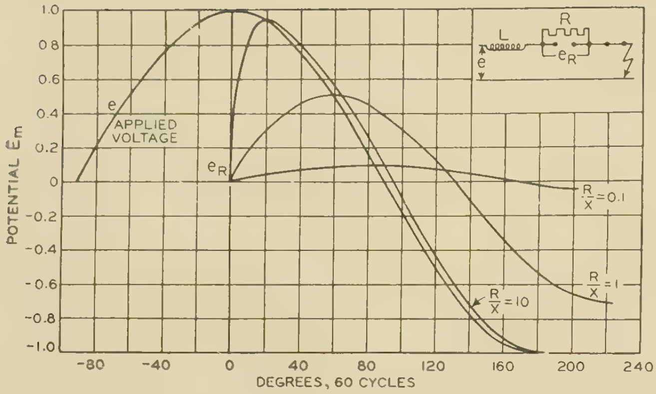


Figure 9. Voltage transients across a gap inserting resistance in an inductive circuit

The sum of these two currents flows through the inductance and the conditions upon which equation 8 is based are no longer true. Consequently, equation 5, 6, or 7 must be used to express the relations. For values of r above the critical value of resistance, the circuit is oscillatory and the crests of the voltage recovery transient exceed the crest of the applied voltage. Except for values of resistance close to the critical resistance, the voltage recovery rate is substantially the same as if no resistance were used across the breaker. Consequently, resistances intended to facilitate interruption must be chosen between definite limits; they must be low enough to be easily inserted by the first gap and high enough to limit the current to values which can be readily broken by the main interrupter.

If the resistance is properly chosen for the maximum interrupting capacity, its insertion during operations at currents below the rated interrupting capacity cause no more difficult conditions because r/x is smaller and the voltages across the resistance are lower. For low currents at higher power factors the voltage drop across the resistor is only a fraction of the drop across the total circuit resistance.

The resistor may be inserted by an arc rupturing device having a high arc voltage, which effects the transfer during the course of the cycle rather than at the current zero. As soon as voltage appears across the connections to the resistor a part of the current is transferred to the resistor. An arc voltage maintained higher than the voltage drop in the resistor will result in the transfer of all the current from the arc, without waiting for an arc-current zero.

The resistor makes the final circuit much easier to interrupt. That high-power-factor circuits are easily opened is generally known. Typical transients showing why this is true are given in figure 10 for various values of r/x from 0.125 to 2. All the curves are drawn for the same values of L and C which have been assumed very large. The transients have a natural frequency of only 900 cycles per second and are spread sufficiently to be easily studied. These curves show clearly that the insertion of a resistor in a reactive circuit reduces the crest value of the transient and also the voltage recovery rate and consequently facilitates the interruption.

The interrupting performance of a resistance-inserting breaker is practically independent of the severity of the circuit recovery-voltage characteristics because the voltage transients across its contacts are controlled largely by the resistor.

With charging currents the resistor can reduce the magnitude of the overvoltages which occur during interruption. The resistor may be inserted by a high arc drop in a manner similar to that on an inductive circuit. If it is inserted at a current zero, the voltage which appears across the parallel gap increases slowly and to a magnitude determined by the relation of the resistance to the inductance and the capacitance of the circuit. If inadequate gap is available a restrike will occur. After the resistance is inserted, the capacitance is charged through the resistor and the voltage of the capacitance is lower than the applied voltage. Any restrike across the second gap occurring before adequate contact separation is obtained will be through the resistor and will be damped by it. Thus the resistor facilitates the extinction of charging currents by reducing the voltage of the capacitance and by damping transients if they occur. Consequently, the resistor reduces the overvoltages which can occur.

Except when the resistor provides the cheapest means of increasing the upper limit of the interrupting ability of a circuit breaking device, its complication is undesirable. Usually, the extension of existing designs will produce a simpler and cheaper means of obtaining the desired interrupting ability.

Arc Resistances in Circuit Breakers

The resistances of the arc spaces in circuit breakers play a part in the damping of switching transients. For example, during the interruption of charging current, the arc strikes through an appreciable distance and introduces arc resistance into the circuit. The arc resistance depends upon current and the arc voltage varies with the arc length. Consequently, the resistance of the arc space exerts an appreciable damping effect upon the transient produced by the restriking.

That the resistance of the arc space may be sufficiently low to influence the voltage recovery transient on inductive circuits was demonstrated in a paper presented before the Institute in 1933. The demonstration that current flowed through these arc spaces during the recovery transient was based upon cathode-ray oscillograms. Four oscillograms were shown which gave approximately the same range and type of transients as those represented in figure 4 of this paper. The transients were not exactly

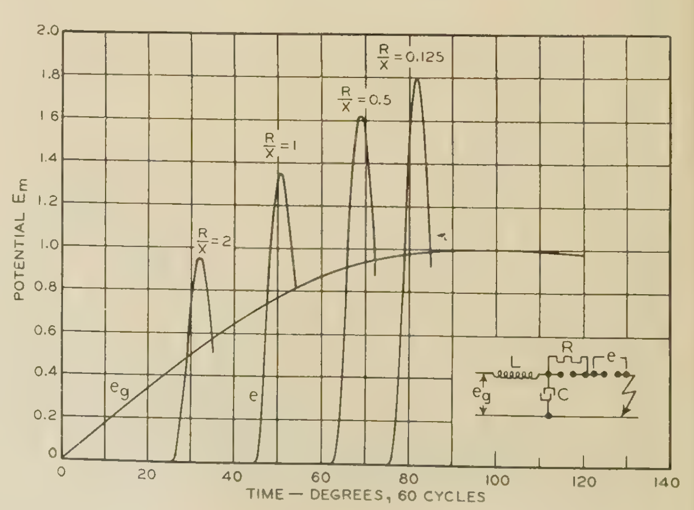


Figure 10. Voltage transients on a gap interrupting an inductive circuit into which a resistance has been inserted

the same however, since the resistances producing the damping were variable instead of constant.

Summary

An inspection of switching transients shows that the resistances of the circuits play an important part in limiting the amplitudes and the durations. Although only a few typical circuits have been analyzed, the general damping effect of resistance has been shown. The calculation of more complicated circuits becomes extremely difficult if the resistances, reactances, and capacitances are all considered. Consequently, calculations of more complicated circuits generally are limited to a consideration of the inductances and capacitances only. The damping of the transients is not included in the calculations and approximations for them have to be made. It is believed that the charts presented in this paper will help in choosing suitable damping factors.

In the design of circuit breakers resistors can be used to increase the interrupting ability of the circuit breaker at the expense of simplicity in construction.

The resistances of the arc spaces of circuit breakers can appreciably influence switching transients by increasing the damping of the circuit.

Appendix

The equations given below are the basis for the curves in the illustrations.

CLOSING TRANSIENTS OF INDUCTIVE CIRCUITS

$$i = \frac{E_m}{Z}\left\{\cos(\theta - \theta_0 - \theta_1) - \left[\cos(\theta_0 + \theta_1) - \frac{i_0 z}{E_m}\right]\epsilon^{-\frac{r}{x}\theta}\right\} \quad (1)$$

E_m = crest value of applied voltage

i_0 = i for $\theta = 0$, the start of the transient

r, x, and z are the resistance, reactance, and impedance of the circuit

θ_0 = phase angle of voltage at $\theta = 0$

θ_1 = $\tan^{-1}\frac{x}{r}$

CLOSING TRANSIENTS ON CAPACITIVE CIRCUITS AND OPENING TRANSIENTS ON INDUCTIVE CIRCUITS

For $R > 2\sqrt{L/C}$

$$e = E_m\left\{1 - \frac{LC}{\sqrt{R^2C^2 - 4LC}} \times (n_1\epsilon^{-m_1t} - m_1\epsilon^{-n_1t})\right\} \quad (2)$$

$$m_1 = \frac{RC + \sqrt{R^2C^2 - 4LC}}{2LC}$$

$$n_1 = \frac{RC - \sqrt{R^2C^2 - 4LC}}{2LC}$$

For $R = 2\sqrt{L/C}$

$$e = E_m\left\{1 - \left(1 - \frac{Rt}{2L}\right)\epsilon^{-\frac{Rt}{2L}}\right\} \quad (3)$$

For $R < 2\sqrt{L/C}$

$$e = E_m\left\{1 - \frac{2\sqrt{LC}\epsilon^{-\frac{Rt}{2L}}}{\sqrt{4LC - R^2C^2}} \times \sin\left(\frac{\sqrt{4LC - R^2C^2}}{2LC}t + \tan^{-1}\frac{\sqrt{4LC - R^2C^2}}{RC}\right)\right\} \quad (4)$$

OPENING TRANSIENTS OF INDUCTIVE CIRCUITS HAVING BOTH SERIES AND SHUNT RESISTANCES

For $\frac{R}{L} - \frac{1}{R_1C} < \frac{2}{\sqrt{LC}}$

$$e = E_mR_1\left\{\epsilon^{-\frac{1}{2}\left(\frac{R}{L} + \frac{1}{R_1C}\right)t}(A_1 \sin bt + B_1 \cos bt) + C_1 \cos(\omega t + \alpha)\right\} \quad (5)$$

$$A_1 = \frac{C_1}{b}\left[\omega \sin\alpha - \frac{1}{2}\left(\frac{R}{L} - \frac{1}{R_1C}\right)\cos\alpha\right]$$

$$B_1 = -C_1 \cos\alpha$$

$$C_1 = \sqrt{\frac{1 + R_1^2\omega^2C^2}{(RR_1^2\omega^2C^2 + R + R_1)^2 + \omega^2(R_1^2\omega^2C^2L + L - R_1^2C)^2}}$$

$$b = \frac{\sqrt{4LC - \left(\frac{L}{R_1} - RC\right)^2}}{2LC}$$

$$\theta = \tan^{-1}\frac{\omega L}{R} + \frac{3\pi}{2} \text{ or } \tan^{-1} - \frac{R}{\omega L}$$

$$\psi = \tan^{-1}\frac{\omega L(R_1^2\omega^2C^2 + 1) - R_1^2\omega C}{R(R_1^2\omega^2C^2 + 1) + R_1}$$

$$\gamma = \tan^{-1}\omega CR_1$$

$$\alpha = \theta - \psi - \gamma$$

For $\frac{R}{L} - \frac{1}{R_1C} > \frac{2}{\sqrt{LC}}$

$$e = E_mR_1C_1\left\{\epsilon^{-\frac{1}{2}\left(\frac{R}{L} + \frac{1}{CR_1}\right)t} \times (D_1\epsilon^{ft} + D_2\epsilon^{-ft}) + \cos(\omega t + \alpha)\right\} \quad (6)$$

$$D_1 = \frac{1}{2f}\left\{\left[-\frac{1}{2}\left(\frac{R}{L} + \frac{1}{CR_1}\right) - f\right] \times \cos\alpha - \omega \sin\alpha\right\}$$

$$D_2 = -D_1 - \cos\alpha$$

$$f = \sqrt{\frac{(L - RR_1C)^2 - 4LCR_1^2}{2LCR_1}}$$

For $\frac{R}{L} - \frac{1}{R_1C} = \frac{2}{\sqrt{LC}}$

$$e = E_mR_1C_1\left[\epsilon^{-\frac{1}{2}\left(\frac{R}{L} + \frac{1}{CR_1}\right)} \times \left\{\left[\omega \sin\alpha - \frac{1}{2}\left(\frac{R}{L} + \frac{1}{CR_1}\right)\cos\alpha\right]t - \cos\alpha\right\} + \cos(\omega t + \alpha)\right] \quad (7)$$

The voltage across a gap inserting a resistor in an inductive circuit, figure 9, is given by equation 8. This equation assumes that the resistance-inserting gap is an arc-rupturing device which has a negligible arc voltage and which interrupts at a normal current zero.

$$e = \frac{E_m r}{x\sqrt{1 - \left(\frac{r}{x}\right)^2}} \times \left[\cos\left(\theta - \theta_1 - \cot^{-1}\frac{r}{x}\right) - \epsilon^{-\frac{r}{x}\theta}\cos\left(\theta_1 - \cot^{-1}\frac{r}{x}\right)\right]$$

E_m is the crest value of the 60-cycle voltage wave

$\frac{r}{x}$ is the ratio of the resistance and reactance of the circuit

θ_1 is the phase of the applied voltage at the start of the transient

If the extinction voltage is not negligible an additional term is inserted after $\cos\left(\theta_1 - \cot^{-1}\frac{r}{x}\right)$.

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Discussion

W. F. Skeats (General Electric Company, Philadelphia, Pa.): Mr. Van Sickle is to be congratulated upon the clarity of his exposition of the subject. Undoubtedly many will obtain a much clearer understanding of the influence of resistance from reading this paper.

A warning must be sounded, however, with reference to connecting too closely the rate of decay of the d-c component of short-circuit currents with the power factor. The resistance influencing the former is substantially the d-c resistance of the circuit, whereas the resistance involved in the latter is the a-c resistance and may be considerably higher. Similarly the resistance involved in the damping of the higher-frequency oscillations which occur upon interruption of a circuit, is likely to be considerably greater than the normal-frequency resistance, and must be so in order to bring about the damping that is observed.

It is also easy to make a mistake in the consideration of load resistance. A synchronous motor, for instance, even though it may be operating at unity power factor must be represented for consideration of transients not as a resistance, but as the combination of a reactance and a generating source. Lighting and heating loads are pure resistance but may not be very effective for damping because of the reactance of transformers and other apparatus which must in general be connected between these loads and the circuit breaker.

Mr. Van Sickle's statement at the end of the section on "Opening Transients of Capacitive Circuits," "These voltages are harmless and indicate that on a typical system the high overvoltages obtained theoretically by neglecting the dielectric characteristics of the breaker, are seldom encountered," seems contradictory to conclusion 5 of the paper by Messrs. Evans, Monteith, and Witzke (AIEE Transactions, volume 58, 1939, pages 386–97) which, after a discussion of overvoltages based on only a slight modification of the theory discussed by Mr. Van Sickle, reads, "The results presented in this study are believed to provide an explanation for some of the line and neutral point flashovers . . . that have been experienced on actual systems."

R. D. Evans, A. C. Monteith, and **R. L. Witzke** (all of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): When considering the broad subject of switching transients, it is always of interest to compare field tests with the analytical results. Mr. Van Sickle has included test data of the line-to-ground voltage for the condition of opening charging currents on a 230-kv solidly grounded system. The oscillogram to which Mr. Van Sickle refers shows seven restrikes and a maximum voltage of about 250 per cent of normal crest. The voltage obtained falls in the range for similar conditions given in figure 13 of our present paper (AIEE Transactions, volume 58, 1939, pages 386–97) which shows 230 per cent for one restrike and 320 per cent for two restrikes. In considering this comparison it is to be noted that the system under test was of considerable greater total mileage but that only one-third of total was de-energized by the switching operation. These figures are in reasonable agreement and emphasize the statement in our paper that there may be a large number of restrikes in the actual case of interrupting the circuit but only a limited number are significant in producing the overvoltage.

(See also discussion, page 412.)

R. C. Van Sickle: Mr. Skeats brought out a very valuable point in emphasizing the need for choosing the correct values of inductance and resistance in the analysis of circuit transients. The values used should be those which correspond to the frequencies of the transient.

In the discussion of the closing transients of inductive circuits, the decay of the d-c component is given as a function of the power factor of the circuit. The frequencies of the transients were 60 cycles or less and the assumption was made that the values of the resistances and inductances were the same for both components of the transients. This is a good approximation not only for the simple single-phase circuit which was discussed but also for three-phase circuits. The International Electrotechnical Commission Publication No. 56 "I.E.C. Specification for Alternating Current Circuit Breakers," uses the same assumptions in an appendix which gives a method of determining the short-circuit power factor of a test from the decay of the d-c component.

When a more exact determination is desired, both the resistance and the inductance values should be taken more accurately.

The difference in the 60-cycle resistance and the d-c resistance will be determined largely by the skin effect in the larger conductors of the circuit. In parts of the circuit such as the bus structure this may make a 15 or 20 per cent difference but for the entire circuit only a much smaller increase would be expected.

The values of the inductances determining the a-c and d-c components are not the same. The d-c component depends upon the negative-sequence reactance. The a-c component depends upon the direct-axis reactance, and in the simple circuit discussed in the paper, it was assumed constant.

Mr. Monteith and Mr. Evans have shown the agreement of this paper with theirs.

Power-System Voltage-Recovery Characteristics

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ASSOCIATE AIEE

WITH the increased use of protector tubes, a thorough knowledge of the recovery voltages to which they may be subjected has become highly desirable. The current range over which a tube can be expected to function successfully is necessarily dependent on the voltage recovery characteristics of the system in which the tube is applied. Since many systems have a large range of fault currents for the different fault and system conditions, it is essential that the corresponding voltage recovery characteristics be carefully considered. This paper presents the results of an investigation to determine and evaluate the importance of the factors which influence the voltage recovery characteristics of power systems. A very large number of tests were made on a specially designed miniature equivalent circuit representing a transmission line, connected apparatus, and the fault-clearing device. In that a miniature system was used, this investigation is similar to that made by Messrs. Evans and Monteith and presented in two recent papers.[1,2] The results presented in this paper represent refinements and extensions to the understanding of the phenomena which will be of interest to both designers and users of protector tubes or other fault-clearing devices. Factors which have not been evaluated previously are shown to influence the phenomena considerably.

Special electronic devices were developed for the purpose of applying and removing the fault in synchronism with the source voltage. These devices have inherent characteristics which closely simulate the arc voltage characteristics of protector tubes.

The point on the voltage wave corresponding to the instant of application of the fault, is shown to be very significant under certain conditions. When this is varied, results obtained indicate the indefiniteness of a first crest in the transient voltage recovery characteristics and point to the maximum crest, time to maximum crest, and the initial recovery rate as being more descriptive.

In the case of double-line-to-ground faults, the conditions under which they would be most likely to occur on an actual system are considered.[5] The effect of this consideration, and the effect of restriking of the first tube attempting to clear are illustrated. Misleading results are shown to be obtainable if these considerations are neglected.

Generalized results are presented in a form such that they can be applied to any solidly grounded system operating between 13.8 and 138 kv.

Paper number 39-49, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted October 26, 1938; made available for preprinting January 3, 1939.

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The author acknowedges the helpful suggestions of Edith Clarke and S. B. Crary and C. Concordia, which have contributed to the success of this investigation. Acknowledgment is also made of S. B. Farnham's assistance in making many of the laboratory tests.

1. For all numbered references, see list at end of paper.

Conclusions

1. Different angles of fault application materially affect system voltage recovery characteristics. When this factor is considered, the significance of a first crest is lost in many cases, and the results indicate that time to maximum crest and maximum crest voltage are the main system voltage recovery characteristics which lend themselves to summarization. Initial rate of rise of recovery voltage is important, but since it varies greatly with fault location in a given system, no attempt is made to summarize the results obtained for the initial period. Instead, a method of calculating the maximum initial rate of voltage rise is indicated which is adequate for many cases. It should not be inferred from this that the events taking place during the interval of time up to voltage crest are unimportant in the functioning of the tube. Actually, the entire voltage recovery characteristic is important up to the time of maximum voltage reached.

2. In studying double-line-to-ground-fault recovery voltages, the probable angle of fault occurrence in an actual system must be considered. Under certain conditions, much more severe recovery voltages can be obtained in a controlled setup than are likely to be obtained in an actual system. It seems evident that if a tube can interrupt a single-line-to-ground fault in a solidly grounded system where tower-footing resistance is encountered, it generally will be able to interrupt a double-line-to-ground fault.

3. Crest recovery voltages obtained in the solidly grounded systems investigated varied from 1.5 to 1.75 when tower-footing resistance was neglected. The effect of losses in the system (including tower-footing resistance) is to decrease the overshoot for single-line-to-ground faults. It appears that 1.75 is near an upper limit which may be reached in higher-voltage systems while 1.5 would be more likely to be reached in a lower-voltage system. This is in general agreement with field tests and theoretical considerations. It should be recognized, however, that system loads and interconnections generally would tend to reduce slightly the maximum overvoltage reached in an actual case.

4. Neutral grounding resistance produces a decrease in maximum overshoot in a manner very similar to that produced by tower-footing resistance.

5. Neutral grounding reactance increases the time to crest, since the zero-sequence frequency is reduced as more reactance is added in the neutral. The effect upon the crest voltage reached appears to depend greatly upon the length of line involved.

6. The magnitude of the arc voltage corresponding to the arc voltage across the protector tube (over a practical range) appears to have little effect upon the voltage recovery characteristics.

7. Length of line, system voltage, and fault current all play an important part as shown in figure 8 of this paper. It is expected that this information will be helpful as an aid to the proper selection of tubes for solidly grounded systems.

8. The results of this investigation appear to be in general agreement with those published by Messrs. Evans and Monteith,[1,2] although, since the systems studied were different in the two investigations, direct comparisons are difficult to make.

9. While this investigation has been chiefly concerned with recovery voltages obtained when faults are interrupted by protector tubes, it is felt that some of these results may be of value to designers and users of other fault clearing devices as well. The flexibility of a miniature setup of this type with the advantages of electronic switching devices has been, and promises to continue to be, helpful in arriving at a better understanding of numerous closely related phenomena.

Equipment Used

The equivalent system used to represent the transmission line was specially designed for this investigation.[6] This made possible the use of units having characteristics simulating those of an actual transmission line over the desired range of frequencies. One factor which influences the phenomena quite appreciably is the resistance-frequency characteristic of the units representing the transmission line.[7] A three-phase equivalent π section as used in this study representing ten miles of typical line is shown in figure 1. This

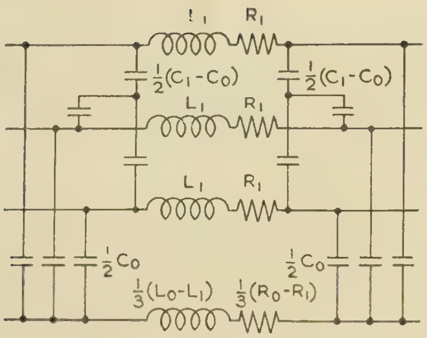

Figure 1. Artificial-line-section connections

is similar to the circuit used in obtaining the results of references 1 and 2.

Source reactances were simulated by means of reactors having the desired characteristics. Transformers were used as shown in figure 2 to make it possible to have a low ratio of X_0/X_1 at the source when desired.

Electronic switching circuits were developed for applying the fault at any desired point on the voltage wave and for interrupting the current or currents at or near the normal current zero. The operation of these circuits is described in the appendix. The circuit used to apply and remove single-line-to-ground faults is shown in figure 3. Use of the thyratron tube to carry the fault current made it possible to represent quite accurately the arc voltage inherent in the protector tube.[4] By variation of the constants, the circuit can be adjusted to apply the fault at intervals of from one to about six cycles, the allowable frequency of repetition depending upon the amount of damping in the circuit under investigation. Once the frequency of fault application has been selected, the phase-shifter can be rotated to vary the point of application of the fault on the voltage wave throughout the range of probable occurrence in an actual system. Thus the effect of point of application of the fault could be investigated easily with this circuit since the thyratron interrupts the fault current automatically at the first subsequent current zero regardless of when the fault occurred. This factor was found to be of particular significance under certain system and fault conditions. The magnitude of the arc voltage could be varied at will by inserting a storage battery in series with the thyratron.

The circuit for studying double-line-to-ground faults is shown in figure 4. A third unit can be incorporated to investigate three-phase faults.

Under some conditions in studying the double-line-to-ground fault, it was desirable to simulate the restriking of the first phase attempting to clear. This was accomplished by means of utilizing a synchronous switch to carry the first loop of current and transferring to the tube on the last loop. A circuit used to accomplish this is shown in figure 5.

A three-phase 110-volt 60-cycle, sine-wave generator was used to energize the miniature system. This was of sufficient capacity so that voltage at its terminals remained essentially constant regardless of transient disturbances imposed in the circuits. Thus auxiliary equipment could be energized from the same source, thereby providing the necessary means for synchronization of fault application.

To observe the transient recovery voltages, an oscilloscope was used. This afforded a convenient means for observing the repeated transient since the

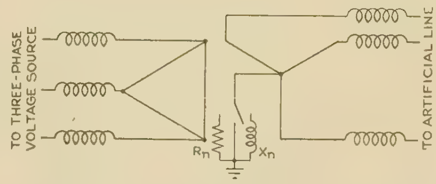

Figure 2. Equivalent circuit for source reactances

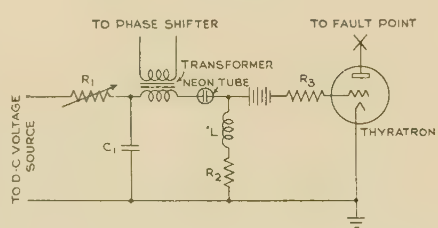

Figure 3. Recurring-fault circuit used in studying transient recovery voltages following single-line-to-ground faults

image could be made to appear stationary on the screen. Time exposure photographs could be taken of this image if desired. However, it was found more practical to take actual measurements directly off the calibrated screen of the oscilloscope.

Discussion of Results

In discussing transient recovery voltages which occur when fault currents are interrupted by protector tubes, it should be emphasized that generally there are two basic differences between these voltages and those obtained in circuit-breaker operation. In the first place, as has been pointed out elsewhere,[1,2,4] protector-tube recovery voltages are generally of a slower nature than those very fast ones sometimes obtained in a circuit-breaker operation which severs one part of a system from another.

As a second point of difference, it should be noted that circuit-breaker interruptions more often occur from a steady-state condition of fault current while a protector tube interrupts generally from a transient condition. Ordinarily a circuit breaker is called upon to operate after fault current has been flowing for several cycles which is of sufficient duration for the natural frequencies to disappear from the fault current and for the d-c offset to be greatly reduced. Protector tubes usually operate in a single half cycle, and therefore, in many cases the natural frequency oscillations in the fault current may not be damped out when interruption takes place. The amount of this transient disturbance present (and also the amount of d-c offset) for a given case is a function of the point on the voltage wave at which the fault occurs.

The inherent flexibility which the fault-simulating circuit possesses made it possible to investigate the effect of fault angle. Figure 6*a* shows several single-line-to-ground-fault recovery voltage transients, each one for a particular value of the angle of fault application as indicated. The fault angle is designated as θ_A which is so defined that a value of

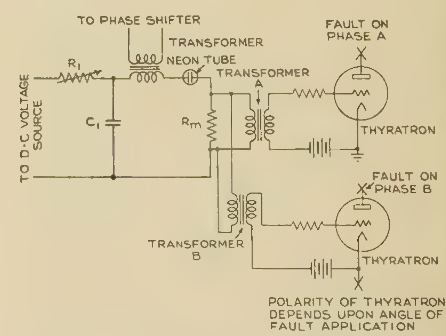

Figure 4. Recurring-fault circuit used in studying transient recovery voltages following double-line-to-ground faults

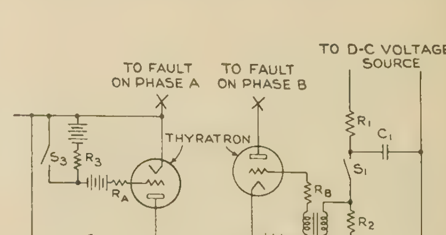

Figure 5. Recurring-fault circuit used in studying transient recovery voltages following double-line-to-ground faults

S_1, S_2, and S_3, are synchronous mechanical switches. Phase *A* carries two loops of fault current and phase *B* carries one

$\theta_A = 90$ degrees corresponds to the time at which the steady-state voltage on phase A is a maximum, the single-line-to-ground fault always being applied on phase A. Values of θ_A from 0–90 degrees indicate application of the fault before voltage crest is reached, while values of θ_A from 90 degrees—180 degrees indicate application of the fault after the voltage crest. Figure 6*c* shows the crest values of the curves in 6*a* plotted as a function of θ_A. The maximum crest recovery voltage was obtained for $\theta_A = 78$ degrees. In this case, line losses were high enough so that the natural-frequency oscillations in the fault current were damped out before interruption took place.

Figure 6*b* shows several recovery voltage transients as obtained on the miniature system representing 100 miles of low-loss line and source reactances as indicated. In figure 6*d* the crest values reached in figure 6*b* are plotted as a function of θ_A. For this case, the maximum recovery voltage of 1.7 was reached for either of two values of θ_A, namely 88 degrees and 120 degrees, while in between these two peaks lies a low value of 1.2 at 108 degrees. This characteristic of alternate peaks and valleys in the crest recovery voltage versus θ_A curve is particularly marked in the case of low-loss lines and for low values of fault current.

From observations of the transient fault current, it was possible to associate these peaks and valleys with the oscillations in the fault current which were of a frequency essentially inversely proportional to the length of line represented in the miniature system. Corresponding field tests indicate that this frequency corresponds to the frequency with which a voltage (or current) surge travels four times over the line length involved.[3] The number of such traversals which can take place during the time that the fault is on is a function of the angle at which the fault is applied. Further analysis indicated that each peak of recovery voltage in figure 6*d* could be associated with fault-current interruption at an instant of minimum rate of change of fault current, and each valley with interruption at a maximum rate of change of fault current. Also, since initial rate of rise of recovery voltage is proportional to the rate of change of fault current just prior to interruption, it was possible to associate a maximum initial rate of rise with fault interruption at a maximum rate of change of current and a minimum initial rate of rise with fault interruption at a minimum rate of change of current. If a system has losses low enough so that these fault-current natural-frequency oscillations exist when the first normal current zero is reached, these alternate peaks and valleys in the crest recovery voltage versus θ_A curve will occur.

The significance of the curves of figure 6 is that in general it is necessary to determine a recovery voltage envelope obtained by applying the fault over a range of values of θ_A. It is conceivable that a single-line-to-ground fault could occur for any value of θ_A between 0 and 180 degrees. However, it has been found that the recovery-voltage envelope is generally determined by values of θ_A between 70 degrees and 110 degrees approximately.

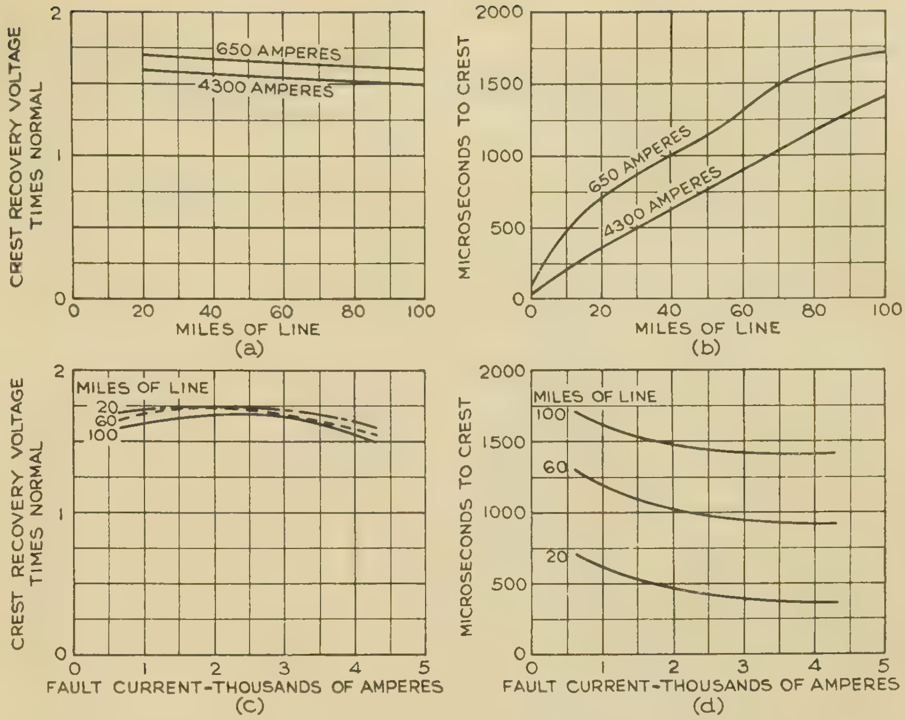

Figure 7. Effect of line length and fault current on recovery voltage. $X_0/X_1 = 0.4$ at source. Currents are root-mean-square symmetrical values for line-to-ground fault in 115-kv system

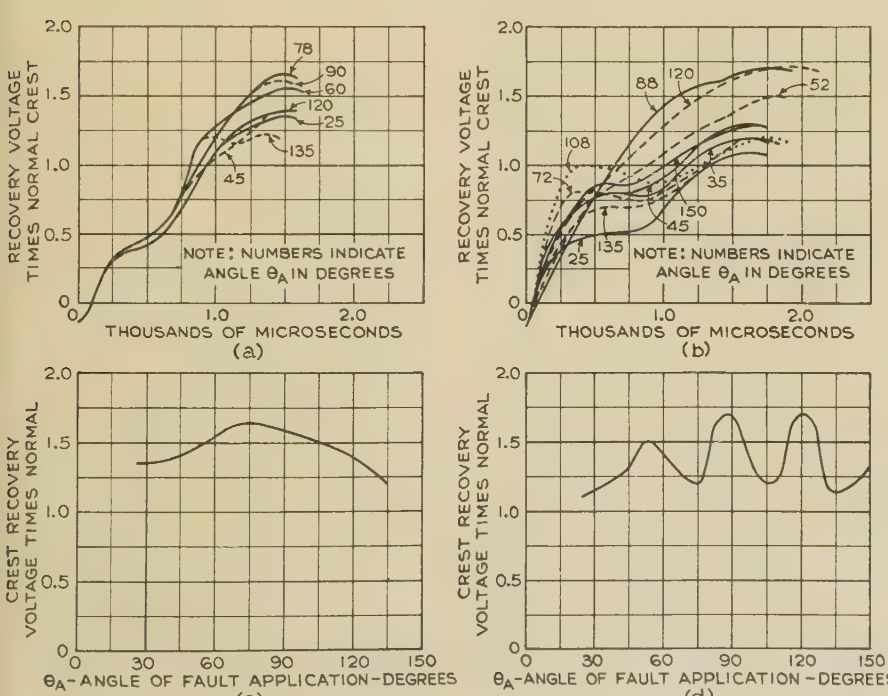

Figure 6. Effect of fault angle on recovery voltage. $X_0/X_1 = 0.4$ at source supplying 650 amperes root-mean-square symmetrical line-to-ground fault current in 115-kv system

(*a*) and (*c*)—60 miles of high-loss line

(*b*) and (*d*)—100 miles of low-loss line

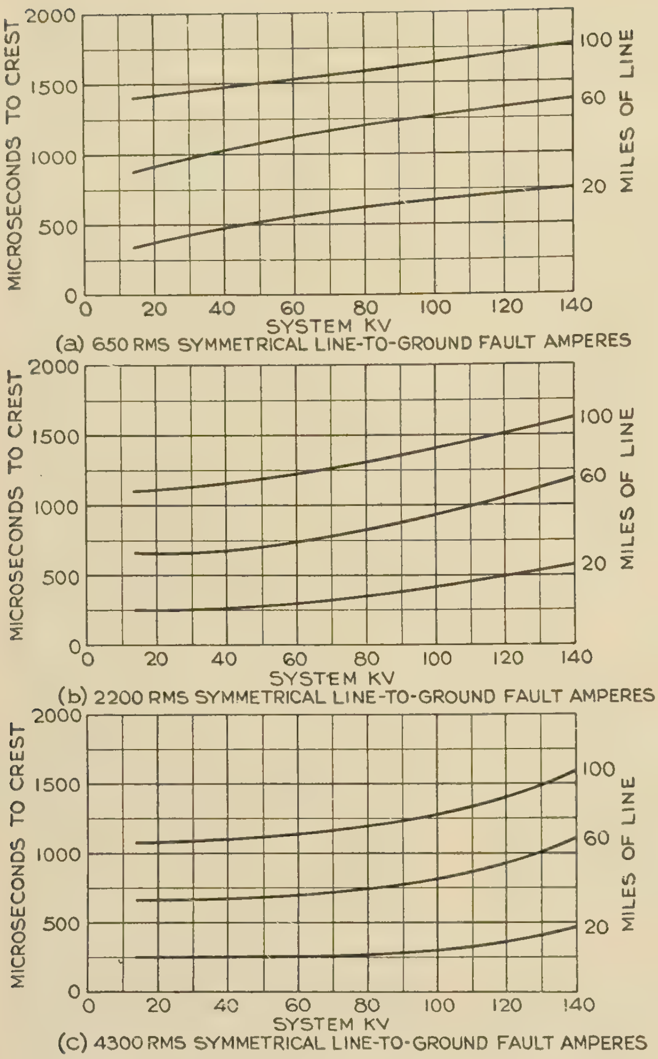

Figure 8. Time to maximum crest recovery voltage for solidly grounded systems. X_0/X_1 = 0.4 at source. Single - line - to-ground fault

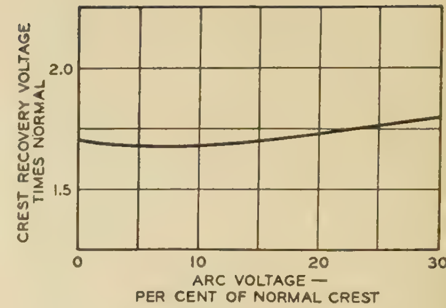

Figure 9. Effect of arc voltage. X_0/X_1 = 0.4 at source. One hundred miles of line. Six hundred fifty amperes root-mean-square symmetrical line-to-ground fault current in 115-kv system at source

When all possible angles are considered, in many cases the identity of a first crest is lost in a more or less smooth envelope rising to a maximum crest value of recovery voltage at a definite time for a given case.

The entire recovery-voltage envelope is of importance, and for successful operation it is essential that the recovery dielectric strength within the tube should be greater at all times than the voltage recovery characteristics. Since it is difficult to summarize recovery characteristics during the initial period, only the maximum crest value in the recovery-voltage envelope and the time required to reach this value are indicated in the summary of results.

There is another reason for using only the maximum crest value reached as an indication of the severity of recovery voltage. It was found that the initial period of recovery voltage is quite definitely a function of fault location along the line while the maximum crest voltage reached and the time to maximum crest voltage were not greatly affected by fault location. The reason for this is that the initial transient period following interruption is governed by the rate of change of fault current just prior to interruption. Fault current changes with fault location along the line, being greater if near a source and becoming less the more remote the fault location is with respect to the source of voltage. Therefore, the initial voltage recovery rate is greater if the fault is near the voltage source, and decreases as the fault location becomes more remote from the voltage source. Since the time to maximum crest recovery voltage is a measure of the natural frequency of oscillation of the entire system, it is not subject to much change with change in fault location. Therefore, it appears to be more descriptive as a system characteristic than the first crest or time to first crest.

The initial rate of rise of recovery voltage can be approximately evaluated for many cases by a relatively simple calculation, based on injecting the symmetrical fault current back into the network at the point of fault.[8] If the short-circuit current at any point along a line is known, then the initial rate of rise is

$IZ\omega \times 10^{-6}$ volts per microsecond

where

I = crest value of the symmetrical fault current
ω = $2\pi f$ (f is system frequency)
Z = surge impedance viewed from the fault point

The initial rate of rise calculated from this expression is the maximum that can be obtained (neglecting natural-frequency components in the fault current) since a symmetrical current wave is assumed. For any instant of fault occurrence which would give a d-c offset component in the fault current, the initial rate would be less. For any system having low losses such that the natural-frequency oscillations would not be damped out when interruption occurred, the maximum initial rate would be higher. In such cases, the maximum initial rate would be difficult to calculate.

Since single-line-to-ground faults are more likely to occur than any other type, and also, since the double-line-to-ground faults which are most likely to occur are generally less severe from the standpoint of tube operation, most of the results presented are for single-line-to-ground-fault conditions. After the effects of several factors had been investigated, the system selected for the first part of the investigation included the source reactances and the line open at the far end with no load. Most of the work was done at the sending end of the transmission line.

Figure 7 shows the effect of line length for several values of fault current in amperes referred to a 115-kv system. The curves are approximately valid for a system of any voltage provided that the fault currents are changed in proportion to the change in system voltage. In other words, it is possible to define a simple system such as these studied here more

generally in terms of X_1 and X_0 at the source, but it is believed that the results are of more significance when plotted with amperes as a parameter. The results indicate that lower currents and shorter line lengths at a given system voltage tend to give higher crest values of recovery voltage in solidly grounded systems. Time to crest increases with line length. This can be explained by the fact that voltage reflections from the far end of the line return less frequently than for shorter lengths and therefore a longer time is required for this building up process. Higher currents reduce the time to crest since the short-circuit current in the miniature systems studied is a measure of source reactance. An increase in current is associated with a decrease in source reactance, which in turn increases the natural frequency of the system and consequently results in a shorter time to crest voltage when interruption takes place.

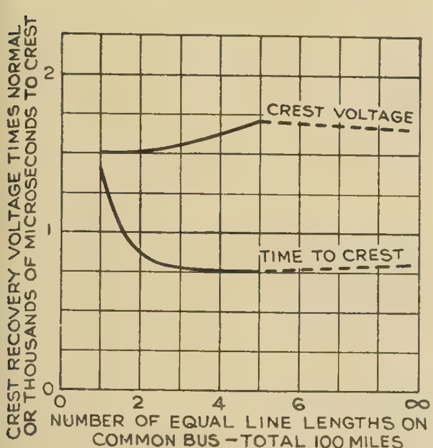

Figure 10. Effect of line grouping. X_0/X_1 = 0.4 at source. Four thousand three hundred amperes root-mean-square symmetrical line-to-ground fault current in 115-kv system at source

Figure 8 is a more comprehensive summary of results obtained for various lengths of line, for various system voltages, and for three values of fault current. These curves are of importance in that they show clearly the effect of varying any one of the three factors. The curves are valid for the relatively simple solidly grounded systems studied, but the qualitative effects of the factors involved can be shown to be valid in general for the more complicated systems on which tubes may be desired. Therefore, this summary may be considered a guide to proper selection of protector tubes. Crest recovery voltages are not summarized since tests indicated that in solidly grounded systems, line-to-ground-fault recovery voltages do not vary greatly unless influenced by losses, either in the connected apparatus or in the ground (tower-footing resistance). Since this summary in figure 8 excludes the effects of these factors, the crest recovery voltages for any of these cases is probably between 1.5 and 1.75 with 1.65 to 1.7 being typical for higher-voltage systems and 1.55 to 1.6 being typical for lower-voltage systems.

It was of interest to check some of the results shown in figure 8 against calculated results based on injecting the fault current into the network at the fault point and considering the distributed constants of the line. When this was done for several cases, it was found that calculations based on single-circuit traveling-wave theory[3] were in essential agreement.

Figure 9 shows the effect of varying the magnitude of arc voltage in the fault. Over a reasonable range, this factor has little effect on the crest value of recovery voltage for a given case. However, it can be shown that varying this factor does change the fault angle which gives the maximum overshoot.

Figure 10 shows the effect of line grouping on a common bus. This curve shows that for a given aggregate number of miles of line, the time to maximum crest recovery voltage is materially reduced as the line is broken up into more short sections. A minimum value is reached, however, which corresponds to lumping the capacitance of all lines at the point of fault. This fact suggests the possibility of calculating recovery voltages approximately using lumped constants when no long lines are involved.

Figure 11 shows the effect of neutral grounding through resistance. The qualitative effect is very similar to that observed when tower-footing resistance is inserted (see figure 15). This probably is to be expected since the circuit connections differ only slightly for the two conditions.

The effect of reactance grounding is shown in figure 12. Both maximum overshoot and time to crest are significantly altered by this factor. As more reactance is added, time to crest becomes greater because of the lower zero-sequence frequency. This soon becomes the predominant factor in determining both time to crest and maximum overshoot, particularly for the short lines where the effect of the distributed constants of the line become relatively small.

Figure 13 shows the effect of line length and short-circuit amperes when a line is fed from both ends. Qualitatively, the effect is very similar to that shown in figure 8 for lines fed from one end.

The effect of fault location along a line receiving power from both ends is of interest. Figure 14 shows that there is little change in crest recovery voltage, but that there is a significant change in time to maximum crest as the fault point is moved along the line. This effect was much less pronounced for lines fed from only one end. However, a fault at the midpoint of a line fed from both ends in this case is in effect the same as a fault at the far end of two 50-mile lines in parallel and bussed at both ends. Therefore, it appears that the effect shown in figure 10 is playing an important part.

The effect of tower-footing resistance for a line fed at both ends is shown in figure 15 for a single-line-to-ground fault.

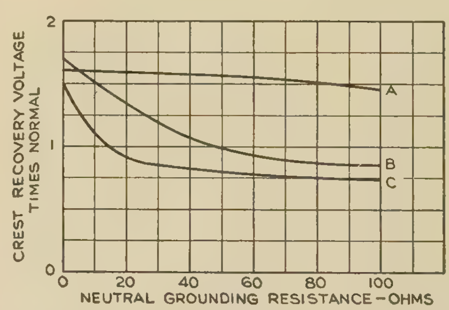

Figure 11. Effect of neutral grounding resistance. One hundred miles of line. X_0/X_1 = 0.4 at source

A—650, *B*—2,200, *C*—4,300 root-mean-square symmetrical line-to-ground fault amperes in 115-kv system for zero neutral resistance

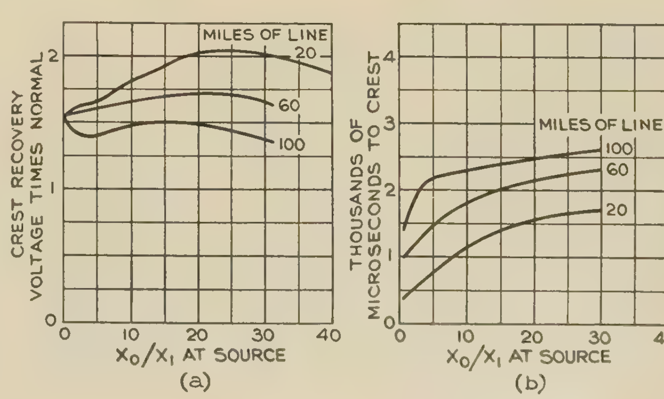

Figure 12. Effect of neutral grounding reactance. Single-line-to-ground fault of 4,300 amperes root-mean-square symmetrical at source in 115-kv system for X_0/X_1 = 0.4

The crest voltage reached is seen to be reduced as the tower footing resistance is increased, and the extent of this effect is greater for the larger available short-circuit currents.

The effect of tower-footing resistance for a line fed at both ends is shown in figure 16 for a double-line-to-ground fault. Three pairs of curves are shown, each set corresponding to certain conditions of fault clearing as indicated.

In the miniature system it was possible to apply the fault simultaneously on phases A and B at any instant in region 1 of figure 17. In an actual case, double-line-to-ground faults would be quite unlikely to occur in a portion of this region. Present understanding of the behavior of lightning discharges in initiating power-system faults indicates that region 2 of figure 17 would probably be much more susceptible to double-line-to-ground faults since the polarities of the two phases involved are alike while the polarity of the third phase is of opposite sign.[5] Under these conditions, phase A will carry only a small minor loop of current prior to the first current zero in that phase. This means that quite likely it will not clear when it tries to interrupt first. Therefore tests made for faults applied in region 2 permitted phase A to carry current until the second current zero was reached. The curves marked A-2 and B-2 were obtained when this procedure was followed. Under these conditions phase B cleared first, although with appreciable tower-footing resistance, the difference in time of clearing of the two phases became very small. If the preceding assumptions are correct, the curves A-2 and B-2 of figure 16 in comparison with the curves of figure 15 indicate that in an actual case, the single-line-to-ground-fault recovery voltage is generally more severe than the corresponding double-line-to-ground recovery voltages where tower-footing resistance plays an important part in determining the minimum values of current to be obtained.

The two curves labelled A and B are the maximum recovery voltages on phases A and B respectively obtainable for applying the fault simultaneously at any instant on phases A and B, and each phase clearing in proper sequence at its first subsequent current zero. Curve A shows the highest recovery voltage reached on phase A for a fault applied simultaneously on phases A and B at such an instant that voltage on phase A was in the vicinity of maximum. Curve B shows the highest recovery voltage reached on phase B for a fault applied simultaneously on phases A and B at such an instant that voltage on phase B was in the vicinity of maximum. Phase B as defined will be carrying much less current than phase A for a practical range of tower-footing resistance, and in fact, may even be carrying less current than would flow for a single-line-to-ground fault. Analysis of figures 15 and 16 indicates that the double-line-to-ground fault under these conditions may be much more difficult to interrupt than the single-line-to-ground fault for certain values of tower-footing resistance.

The preceding analysis indicates that phase B (which would tend to clear first) may be difficult to interrupt. Therefore phase B was permitted to restrike in the miniature system so that phase A would be forced to interrupt first. When this was done, the pair of curves indicated as A-1 and B-1 were obtained. This made the recovery voltage on phase A very high while on phase B it had been reduced. The situation had not been entirely relieved since the recovery voltage on phase A was so high as to cause possible difficulties of interruption even though it was carrying the greater current.

Therefore it appears that if double-line-to-ground faults could occur at any time, without regard to the relative polarities of the phases involved, the recovery voltages associated with this type of fault would be a very important factor in the selection of protector tubes in many cases. It is quite likely, however, that relative polarities determine the conditions under which a double-line-to-ground fault can occur in an actual system, and for that reason the relatively high recovery voltages which can be obtained in a controlled miniature system are seldom, if ever, obtained in an actual system for this type of fault.

Appendix. Operation of Recurring-Fault Electronic Switching Circuits

In figure 3, the fault point designates any place in a network at which the recovery voltage following clearing of a fault at that point is desired. This fault is shown occurring to ground, but in the general case, this need not be true. It may be thought of as occurring between any two points in a network, for example, line-to-line in a three-phase system.

The thyratron in figure 3 can be made conducting at any instant by making the grid sufficiently positive, provided the anode of the tube is sufficiently positive to cause striking of an arc. Essentially, this results in a short circuit between the fault point and ground, and in the miniature equivalent system would represent a fault on the actual system By synchronizing an applied positive impulse to the grid of this tube with the system voltage, the

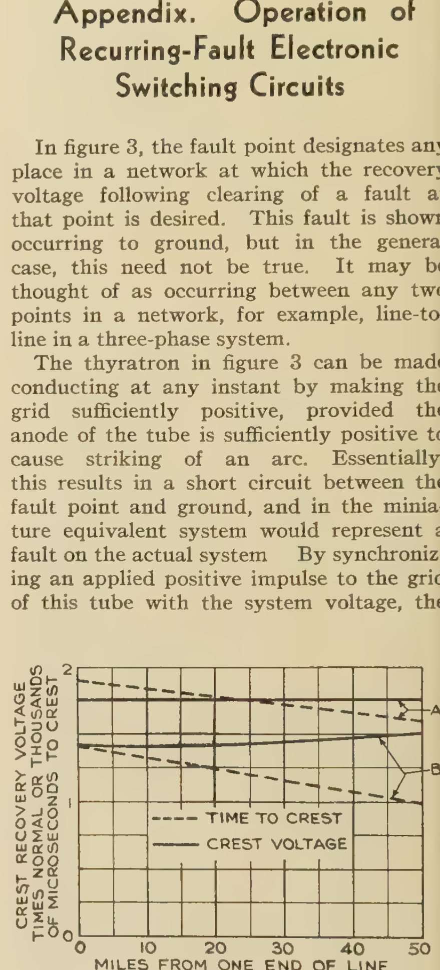

Figure 14. Effect of fault location. One hundred miles of line with equal generating capacity at both ends. Solidly grounded system

A—1,240 root-mean-square symmetrical line-to-ground fault amperes at either end in 115-kv system

B—2,740 root-mean-square symmetrical line-to-ground fault amperes at either end in 115-kv system

fault may be repeatedly applied at any point on the voltage wave. Fault current will flow from the time the fault is applied until the first subsequent current zero when the thyratron will automatically cut off the fault current. The voltage appearing across this tube will correspond to the actual recovery voltage of the system represented.

The thyratron serves to simulate the arc voltage drop of certain devices, such as expulsion tubes, for instance. The amount

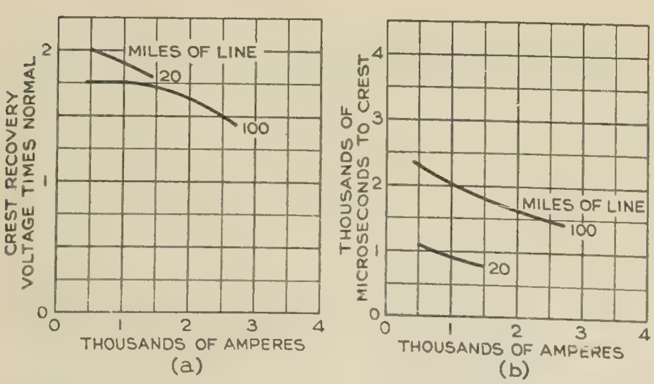

Figure 13. Effect of fault current. Equal generating capacity at both ends of line. Solidly grounded system. Currents are root-mean-square symmetrical amperes for line-to-ground fault at one end in 115-kv system

of arc drop is fairly constant for the tube during the time of current flow. The magnitude of arc voltage for specific cases can be varied by inserting a fixed direct voltage (for example, a storage battery) in series with the tube and the over-all drop can be made greater or smaller than the arc voltage of the tube alone.

The purpose of the portion of the circuit on the left-hand side in figure 3 is to time the application and removal of the fault so that the transient condition is repeated in synchronism with the system voltage. The time constant of the series circuit R_1C_1 can be varied to correspond roughly to the time for two, three, or any desired number of cycles of base frequency (preferably not more than about six). If the neon tube is not conducting, the voltage across C_1 rises exponentially with time when the direct voltage is applied. With the transformer unexcited, this same voltage appears across the neon tube. When this voltage reaches a certain value, the neon tube will suddenly become conducting and discharge the capacitor C_1 rapidly through L and R_2, the time constant of this path being small as compared with R_1C_1. The purpose of L is to prolong the duration of this discharge

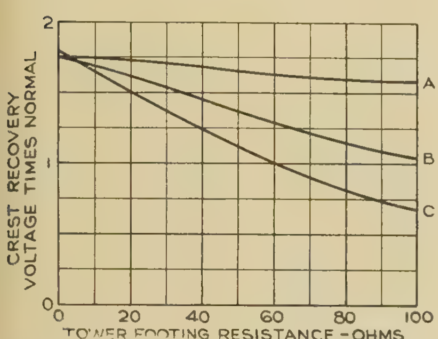

Figure 15. Effect of tower-footing resistance. Equal generating capacity at both ends of line. Solidly grounded system

A—100 miles of line; 414 root-mean-square symmetrical line-to-ground fault amperes at either end in 115-kv system

B—100 miles of line; 1,240 root-mean-square symmetrical line-to-ground fault amperes at either end in 115-kv system

C—20 miles of line; 1,460 root-mean-square symmetrical line-to-ground fault amperes at either end in 115-kv system

so that the thyratron will have ample time to ionize. Immediately after the discharge, the neon tube becomes nonconducting and the cycle repeats itself automatically.

The purpose of the transformer is to add a small alternating component of system or reference voltage. The resultant voltage appearing across the neon tube while it is not conducting is the sum of an exponentially rising voltage and an alternating voltage of reference frequency. Therefore if the time constant R_1C_1 is made such that breakdown voltage of the neon tube is not reached on the second cycle, for instance, but is reached on the third, the frequency of firing the neon tube is made to interlock with the system frequency, and this will be repeated automatically every three cycles.

The frequency of repetition depends essentially on the time constant R_1C_1. If the impulse of voltage appearing across L and R_2 when C_1 is suddenly discharged through the neon tube, is applied to the grid of the thyratron, then the fault condition desired will be repeated in synchronism with system frequency.

The primary of the synchronizing transformer is excited from a phase shifter which is excited from the same source used to energize the miniature system. By ro-

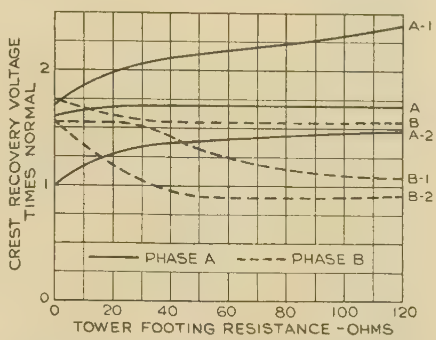

Figure 16. Effect of tower-footing resistance and angle of fault application. Equal generating capacity at both ends of 100-mile line. Double-line-to-ground fault on phases A and B at one end. Solidly grounded system. $I_A = I_B$ = 1,640 root-mean-square symmetrical amperes in 115-kv system for zero tower-footing resistance. See figure 17 for definitions of regions indicated

A and *B*—Maximum for interruption at first current zero

A-1 and *B*-1—Maximum for phase *A* carrying one loop and phase *B* two loops

A-2 and *B*-2—Maximum for phase *B* carrying one loop and phase *A* two loops

tating the phase shifter, the complete circuit can be made to apply the fault repeatedly at any desired point on the voltage wave. When an oscilloscope is used to view the repeated transient, it can be made to appear stationary on the screen.

The tendency of a protector tube to interrupt before normal current zero can be simulated by means of inserting a small resistor in series with the faulting device and synchronizing as above a current surge of controlled magnitude and shape through this resistor. Thus a voltage is built up in opposition to the flow of fault current just prior to interruption so that the fault current is forced to zero sooner than it would be if normal conditions continued to exist. This introduces the characteristic rise in arc drop just prior to interruption of current flow and is of significance in some cases, particularly where the natural frequencies are high. Thus the effect of arc voltage characteristics can be simulated in great detail, not only for protector tubes, but for other interrupting devices as well.

The circuit shown in figure 4 is very similar to that of figure 3 in principle. In this case, two thyratrons are fired simultaneously in synchronism with the system frequency, thus making it possible to simulate double-line-to-ground-fault conditions in the miniature system.

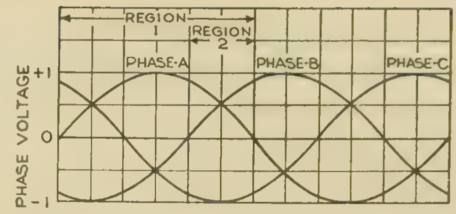

Figure 17. Regions of fault application

The circuit of figure 5 was used in applying double-line-to-ground faults when it was desired to investigate region 2 of figure 17 and permit current to flow in phase A until interruption occurred at the second current zero. The first loop of fault current in phase A was carried by S_2 and the second loop by the thyratron in parallel with it. Since phase B carried only one loop, it was carried entirely by the thyratron. S_1, S_2, and S_3, were all operated by synchronous contactors opening and closing at the proper times to produce the desired switching sequence. The contactors were mounted on an adjustable rack so that the angle of fault application could be varied as desired.

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Discussion

D. C. Prince (General Electric Company, Philadelphia, Pa.): This paper represents a valuable addition to the literature on voltage recovery transients. It is understood to refer primarily to the behavior of expulsion tubes. It has not yet been shown whether rate of rise of recovery voltage, or time to recovery crest and its value or some other characteristic of the recovery transient limits the capacity of certain interrupting devices. In fact, different interrupting devices may well be sensitive to different elements in the recovery characteristic.

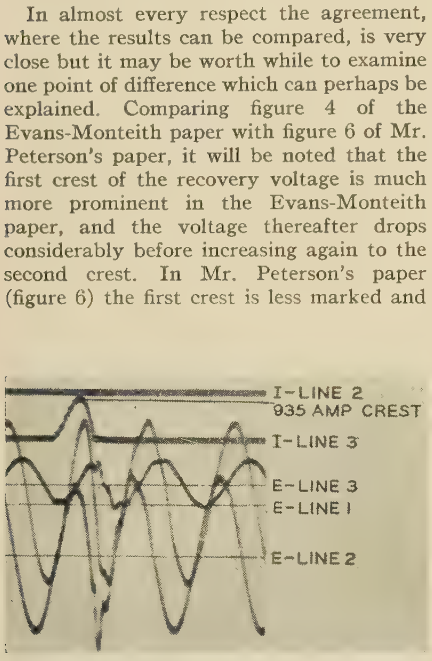
In due course it is hoped that an actual recovery strength curve will be available for

each interrupting device. When that time arrives the true significance of papers such as Mr. Peterson's can be measured.

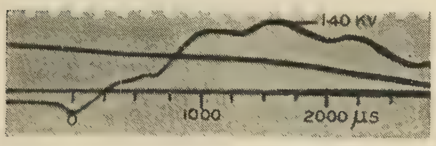
E. J. Wade (General Electric Company, Pittsfield, Mass.): This paper is an excellent contribution to the literature on recovery voltage and Mr. Peterson is to be commended for his exhaustive study of the subject.

Because of the similarity in the results of this paper and that by Evans and Monteith, they mutually support each other although differing in details. This statement has even more significance when it is remembered that the circuit for the line representations and the switching methods were both different.

In almost every respect the agreement, where the results can be compared, is very close but it may be worth while to examine one point of difference which can perhaps be explained. Comparing figure 4 of the Evans-Monteith paper with figure 6 of Mr. Peterson's paper, it will be noted that the first crest of the recovery voltage is much more prominent in the Evans-Monteith paper, and the voltage thereafter drops considerably before increasing again to the second crest. In Mr. Peterson's paper (figure 6) the first crest is less marked and



(*a*) Magnetic oscillogram (voltages measured at generator bus)



(*b*) Cathode-ray oscillogram. Voltage on line 3

Figure 1. Single-phase test on 115-kv circuit. Fault at near end of 60-mile line

thereafter the voltage does not drop before being increased by the reflection from the far end of the line. Wave shapes which have been measured during field tests more nearly approximate the curves given in Mr. Peterson's paper and the question arises as to whether this difference in wave shape is due to a difference in the assumed constants, or to the method of representation of the line.

It is of interest that the data represented by figure 6*a* of Mr. Peterson's paper was obtained with the circuit set up to simulate, as nearly as possible, the conditions found on a field test on the lines of the Boston Edison Company.

The oscillograms for this test are shown in figure 1 of this discussion. In general, the wave shapes, and time to crest, check very closely with Mr. Peterson's results, although there are minor differences in details, which may be attributed to the fact that the 60 miles of line was obtained by looping a 30-mile section of double-circuit line on opposite sides of the same towers. This introduces a small effect due to coupling which was not simulated by the miniature setup.

The current oscillogram also shows oscillations due to the starting transient which in this particular case were entirely damped out before the current reached zero. The measured initial rate of rise of voltage was 190 volts per microsecond which is a satisfactory check of the calculated value of 175 volts per microsecond.

It would be of interest if Mr. Peterson could give some data regarding the relative losses in the high- and low-loss lines as used in figure 6.

I agree with Mr. Peterson that the first crest is not of as much importance as the entire shape of the recovery voltage wave, and because the first crest tends to be obscured in some cases, further agree that it would be preferable to use 90 per cent of the crest of leg voltage as this gives a definite value of voltage at which the corresponding times may be compared.

R. D. Evans, A. C. Monteith, and **R. L. Witzke** (all of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The subject of switching transients has been under discussion for some years. Due to the complexity of the problem, analytical work has been rather limited, the best conclusions being taken from actual system performance. Recognizing the needs for a method of calculation and for further refinement in theories, the a-c network-calculator method or miniature-system method was developed and presented[1] two years ago at the summer convention in Milwaukee. At that time it was introduced as a method that could be used for analyzing recovery-voltage problems, but it was also recognized that it "opened the way to a systematic general investigation of recovery voltage transients and related problems" and presented "a practical method of determining the electrical transients of systems." A review of the discussions presented indicates that at that time the method was considered radical and skepticism was expressed as to its broad usefulness.

It is encouraging to us to note that several papers presented on this subject today make use of this method. It is our firm belief that this method will receive even broader usage.

The paper by Mr. Peterson is of interest in that it so closely parallels our work, the results of which were presented[2] at the last winter convention. Where comparison can be made the checks are surprisingly close. Any differences appear to be due largely to differences in assumptions rather than to differences resulting from the actual application of the method or from the introduction of refinements.

We do not clearly understand Mr. Peterson's statement as to the difficulty of summarizing the first crests and including the effects of initiating transients. A summary of measurements of first crests was presented in our paper.[2] In that work we made adjustments to include the effect of the initiating transients, the fault being applied so as to make the first crest a maximum. We feel that this is essential as in a large number of cases the design of the protector tube is determined by the first crest.

Quite often, depending upon the system characteristics, the recovery voltage curve may have several crests before maximum is reached. In our paper presented last year,[2] we not only summarized the first crest but also the succeeding significant crest, having in mind the protector-tube application. Mr. Peterson apparently has summarized the maximum crest with its corresponding time. The fact that our paper summarized the "significant crest" and Mr. Peterson's paper the "maximum crest" will account for differences in the data. This is a possible explanation of why Mr. Peterson's summary showed relatively higher voltages for the high-voltage systems. It was our opinion, that the times associated with these higher voltages were so long as to make the slightly lower voltage with a considerably shorter time more important in the application of the protector tubes.

In the representation of systems we have given considerable thought to the networks to be used. We found it necessary to weight the constants used when employing a π-type network. In most cases, however, we used a more complicated network with a weighting of the constants proportioned for the particular system being studied. It would be interesting to know whether Mr. Peterson used a simple π network or one with weighted constants for the short line sections.

Electronic devices for controlling the application and removal of the fault are convenient and advantageous where the electronic tube characteristic closely follows that of the arc path desired for the study. This arc characteristic does not, however, lend itself to broad transient analysis. It is for this reason that, after giving consideration to the use of the electronic device and the synchronously driven switch, we chose the latter, preferring to have one device for general investigation.

In eliminating the consideration of the double line-to-ground condition for the application of protector tubes, Mr. Peterson has considered the zone where the instantaneous voltages on two phases are of the same polarity and of practically equal magnitude. It is our opinion that the power-system voltage is low in comparison with that of lightning and therefore that the effects of polarity and magnitude of the normal system voltage are negligible. Lightning can strike any phase, and cause the breakdown of one tube which in the more severe cases will cause a rise in the potential of the ground point and result in a flashback through one or two of the remaining tubes. If this is correct the double-line-to-ground fault can occur at any point and cannot be eliminated from consideration.

There is an intermediate range of tower resistance that makes it possible for the first tube attempting to clear for the double-

line-to-ground fault to hang on and for the second tube to clear. The condition would then be that of a single-line-to-ground fault and the first tube would then clear. Such operation would subject the first tube to considerable erosion. However, for both the lower and the higher values of resistance, the severity on the two tubes is so closely the same that if one fails to clear, the other will probably not clear. We therefore feel that the recovery voltage conditions for double-line-to-ground should be considered in the selection of the tube.

In conclusion it is of interest to point out that no factor has been uncovered in these investigations to question the grounded-neutral system which is in general use in this country.

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2. Recovery Voltage Characteristics of Typical Transmission Systems and Relation to Protector-Tube Application, **R. D. Evans** and **A. C. Monteith**. Electrical Engineering, volume 57, August 1938, pages 432–43.

Harold A. Peterson: The interest aroused in the general subject of overvoltages as indicated by discussions both written and oral reflects the importance of being able to obtain readily quantitative results such as those presented in my paper. The miniature-system method of actual system representation appears destined to continue to play an important part in arriving at a better understanding of numerous closely related transient and steady-state phenomena. Since this practical tool for evaluating voltage recovery characteristics for a variety of interrupting devices under various operating conditions has been developed to a stage of usefulness, it is hoped that similar progress can be made in determining the recovery dielectric strength curves for each interrupting device as well, so that the problem of co-ordination between such curves and voltage recovery characteristics can be placed on a sound engineering basis as Mr. Prince suggests.

In comparing figure 6 of my paper with figure 4 of the Evans-Monteith paper (reference 2), Mr. Wade has brought out a significant point. The following discussion will explain the cause of this point of difference in results and will answer several of the questions raised by Messrs. Evans, Monteith, and Witzke in their discussion. The reason for this point of difference can best be understood by considering various methods of representing the transmission line in miniature. Figure 2 of this discussion shows a family of voltage-recovery curves obtained for an assumed actual system. Each curve corresponds to a different method of representing the actual system as indicated. The fault was left on sufficiently long in each case so that interruption took place from a steady-state condition (that is, there were no initiating transients and only symmetrical power current was flowing). It will be observed that the curve A obtained using nine π sections for the 90 miles of line closely approximates the behavior of a uniformly distributed constant line (curve D). This approximation is very good, even to the timing of the return of the reflected wave from the far end of the line. Since no losses were assumed in calculating curve D, it is to be expected that the crest voltage after reflection would be higher than that actually obtained in any of the miniature system setups.

Curve C obtained for a weighted-constant double-π representation shows a tendency

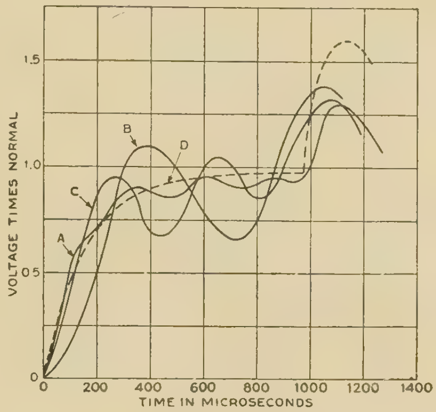

Figure 2

to give a higher first crest voltage than that obtained either with the nine π sections or by distributed-constant calculations. There is a pronounced voltage drop after this first crest is reached before the voltage continues on upward to the maximum crest value. It will be observed also that the axis of oscillation of this first period is the true exponentially rising recovery characteristic.

Curve B shows the recovery characteristic for an equivalent double-π representation (not weighted). This gives a still higher first crest voltage, but the axis of oscillation appears to be approximately the exponentially rising true recovery characteristic.

Several points of interest may be noted in this figure. All three methods of miniature representation considered yield essentially the same time to maximum crest voltage, and only slight discrepancies appear to exist for the magnitude of the maximum crest voltage. The greatest discrepancies occur during the initial period before the reflection returns from the far end of the line. The first crest voltage obtained for a double-π representation, weighted or not, characterized by magnitude and a time to that magnitude, *does not lie on the true system recovery characteristic.* With the nine π section representation there are several oscillations, small in magnitude, which deviate only slightly from the true recovery characteristic. It is for this reason the statement was made that it is difficult to summarize the initial part of the recovery characteristics in terms of a first crest and a time to first crest. This analysis may account for the fact that some of the first crest voltages obtained by Messrs. Evans and Monteith[2] appear to be high. First crests, at least in part (depending on initiating transients), are characteristic of the miniature system representation, and in general, may not be characteristic of the actual system it was intended to represent. As indicated by the curves of figure 2, time to 90 per cent normal voltage would have more significance in interpreting results obtained in our miniature system where each artificial π line section represents ten miles of actual line. Experience, as illustrated by these curves, indicates that for lengths of line as short as 20 miles, such π representation does not give very accurate initial recovery conditions. However, maximum crest and time to maximum crest voltage are not in appreciable error.

It is important to point out that the entire recovery voltage characteristic is important, and therefore any attempt to summarize the severity of the initial period by means of a single magnitude and the corresponding time cannot be entirely adequate.

The low-loss line as used in obtaining results in figure 6 of my paper had an R_1/X_1 and R_0/X_1 ratio of 0.10. The high-loss line had an R_1/X_1 ratio of 0.40 and in addition had a high neutral-return resistance of two ohms per mile to damp out quickly the fault-initiating transients.

Electronic devices for controlling and removing the fault in a miniature system are distinctly advantageous. Fault interruption always takes place precisely at a current zero without adjustment. This is true even for very high natural frequencies of the circuit under investigation. High frequencies present an almost insurmountable difficulty to mechanical devices if flexibility of control for recurring transient conditions is desired. In addition, when electronic devices are used, various arc-path characteristics can be simulated as indicated in my paper. This is of importance where it is desired to know the effect of assumed or known arc characteristics of certain interrupting devices. If the miniature-system voltage base is sufficiently high, the normal arc drop in the thyratron tube becomes insignificant. In cases where such conditions do not prevail the arc drop can be reduced to zero as indicated in the appendix.

It was not my intention to eliminate the double-line-to-ground fault from consideration in the application of protector tubes as Messrs. Evans, Monteith, and Witzke infer. The curves shown in figure 16 were simply intended to illustrate the effects of restriking in the case of a double-line-to-ground fault. A double-line-to-ground fault can occur at any instant regardless of polarity, although the region indicated in figure 17 is probably most susceptible. In case restriking does occur for a double-line-to-ground fault, clearing becomes a selective process. The phase to clear first will then be the one which can be most easily cleared. If the tube is not able to clear the easier phase to interrupt of the two, the fault may continue until cleared by braker action. However, in general, it is necessary to consider every possible instant of fault application and interruption at the first current zero in each phase to insure proper protection over a period of time since any restriking results in excessive erosion of the tube walls.

A New High-Capacity Air Breaker

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Synopsis: Air circuit breakers for lower voltages of simple design are being replaced by improved types, which incorporate specially designed circuit-interrupting devices for the purpose of improving the interrupting efficiency and minimizing the formation of arc flame and gases. A new form of deionizing arc interrupter is described which is equally effective for both d-c and a-c circuits. A new air circuit breaker has been designed to utilize this interrupter in which carbon arcing contacts are replaced by refractory metal, and laminated-brush-type main contacts are replaced by silver-faced solid copper. These improvements have led to greatly increased current-carrying capacity in breakers of a given size and also large increases in interrupting capacity with a minimum of noise and flame, which permits the breakers to be readily mounted in enclosures and cubicles of small physical size.

I. Introduction

INTERRUPTION of low-voltage circuits in air is so easily accomplished by the mere separation of suitable contacts that it has been unnecessary to develop efficient arc-interrupting devices in order to make air breakers workable. The usual breaker construction permits the formation of an unrestricted arc which lengthens due to a self-generated magnetic field until interruption is accomplished, and meanwhile discharges ionized gases in undesirable quantity to the surrounding space. Such simplicity is permissible if adequate mounting space is available for the breaker, and if there is no objection to the thunderous detonation which accompanies the arcing.

Compact metal-clad switchgear, however, has grown up around the conception of adequate circuit breakers which will take care of the gases and other by-products of interruption in the devices themselves without imposing complications on the bus and mounting structure. Standard practice involves air-insulated busses and connections for the most part with conductor insulation only as protection against inadvertent contingencies. The increasing use of air breakers in metal enclosures is, therefore, creating a demand for carefully controlled arc interruption which will confine the arc to deionizing chambers and prevent electrical breakdown between phases or to ground as the result of ionized flame widespread about the breaker.

Deionizing interrupting devices, originally developed by Slepian and associates,[1] have provided a solution to the problem of air interruption in confined space for several classes of breakers,[2] and recently Dickinson,[3] and Sandin,[4] have described compact enclosed breakers utilizing the deionizing principles for opening circuits of moderate short-circuit current. The development of enclosed air breakers which will interrupt currents up to 120,000 amperes root-mean-square and which will have universal application to both d-c and a-c circuits of 750 volts and below, has however necessitated a new form of interrupting device. In addition, the unusual stresses of such high short-circuit currents have made necessary the development of a new breaker itself for the purpose of minimizing contact burning, increasing interrupting speed, decreasing size, and improving mechanical adequacy. Both the interrupting chamber and the breaker are described in this paper.

II. Deionizing Interrupter

Interruption of high short-circuit currents with scant outward display and virtual freedom from external ionized flame requires a confined arc which dissipates the least possible energy. Although the a-c circuit may be opened with a theoretically very small energy dissipation by rapidly deionizing the arc path near a normal current zero,[1] the d-c circuit discharges an amount of energy in the arc somewhat greater than that stored in the circuit electromagnetically when it is interrupted.[5] The d-c energy dissipation and arcing time can be minimized by developing the highest permissible arc drop which will not endanger the circuit insulation immediately upon separating the contacts, and sustaining this value until interruption is complete. The arc drop which is the product of volts per inch and inches length can be achieved either by considerable lengthening of the arc or by the development of a high voltage gradient along its axis. The latter means is preferable first because the necessary arc drop can be developed most quickly by the introduction of deionizing means to increase the voltage gradient, and second because a smaller structure is required if the arc is kept short. The product of arc length and gradient should be kept substantially constant to avoid overvoltages. It is also necessary that the breaker contacts mechanically separate at high speed in order to provide immediately the necessary arc length.

For best interruption of the a-c circuit, the arcing time should also be kept small. On high-voltage circuits it is preferable to permit the arc to continue to a normal current zero, at which point the interruption is completed. On low-voltage circuits, however, it is also practical to interrupt the circuit in a d-c manner by bringing the current to zero with a limited arc voltage. The power loss in the arc may be increased if this is done, but since the arcing time is shortened the energy dissipation with high current and low circuit voltage will not necessarily be increased.

The usual methods of interrupting an arc do not possess all of the characteristics enumerated. Simple lengthening of the arc with a magnetic field does not sufficiently confine it nor limit its length. Furthermore, the volt-time characteristics of magnetically driven arcs do not have the desired shape for d-c interruption. Finally, magnetic-blowout breakers are notoriously noisy.

Interruption by means of gas blast has been very successful in the operation of fuses. Such means are peculiarly difficult to apply to air breakers however, because of the necessity of almost complete restriction about the contacts. Furthermore, gas-blast devices are generally noisy and have a limited life since material is eroded away in forming the necessary gas.

The deionizing arc chamber[2] meets the requirements of a-c interruption very successfully, and is more efficient than other methods in common use. It is not applicable for currents as high as 100,000 amperes, and because of its principle it is not adaptable for the interruption of the higher-voltage d-c circuits.

An arc may be driven into a narrow slot formed by walls of non-gas-forming insulating material, and if sufficient venting is provided for the gases together with sufficient constriction of the arc, considerable arc drop can be developed. Slepian[6]

Paper number 39-34, recommended by the AIEE committee on protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 19, 1938; made available for preprinting December 30, 1938.

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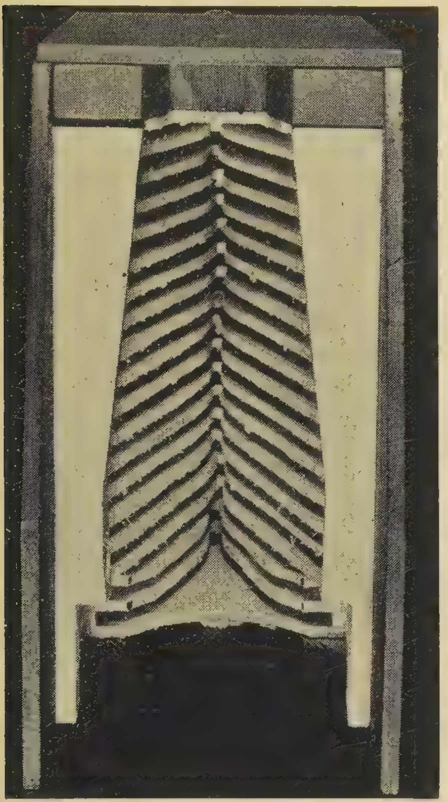
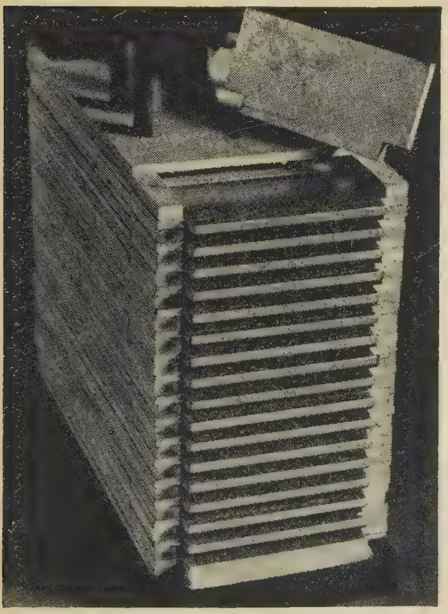
1. For all numbered references, see list at end of paper.

has published data showing that in a one-eighth-inch slot, 800 root-mean-square volts per inch can be interrupted. Deionization chambers made with this principle do, however, fail to limit the arc length and voltage drop, and present problems in venting with very high currents.



(a) Bottom view showing arrangement of insulating plates in arc chamber



(b) Top view showing location of iron plates

Figure 1. Arc chamber

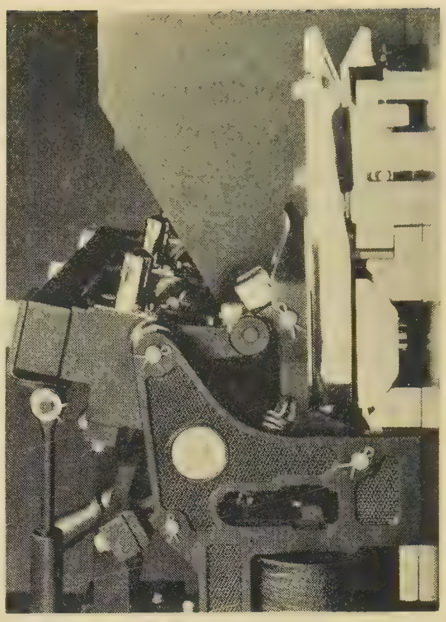
During experiments with arcs in narrowly slotted plates, spaced apart to allow venting, it was found that if the upper part of the slot were closed the magnetic field would drive the arc to the top of the slot, after which the arc core would remain stationary but the field would drive gases turbulently through the arc and deionize it. The construction is similar to one described by Slepian in 1933.[7] In principle the magnetic field acts on the electrons which in turn furnish the gas particles with a resultant velocity by bombardment. Hereby a strong blast of gas is passed through the slots and the arc must, therefore, ionize fresh gas in considerable quantities to maintain a conducting path.

The arrangement finally developed consists of a large number of non-gas-forming insulating plates with V-shaped slots as shown in figure 1. They are spaced with intervals at right angles to the arc path mainly to provide free venting space to the top of the arc chamber for escape of arc gases, thus reducing back pressure which would tend to direct ionized gases downward. It will be noted that the slots are quite narrow at the upper end, while at the lower end they are widened out considerably. This provides room for movement of the arc tips and contact arm. The constriction of the arc in the slots is also important in causing the arc to assume a section which makes the magnetic and gas action most effective.

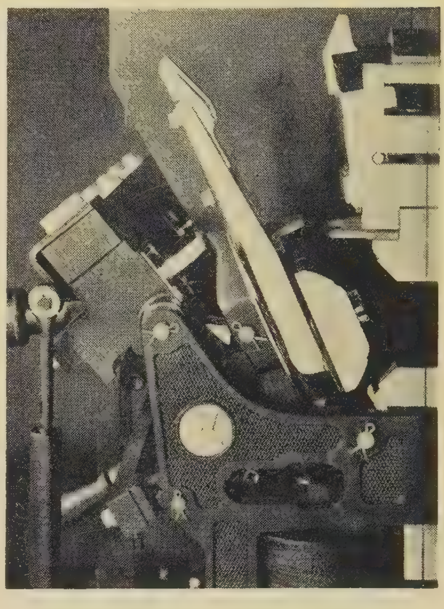
For convenience, the term "magnetic blast" has been adopted to describe the action, and the device is called a magnetic-blast interrupter.

The magnetic field may be provided by a series coil, but it has been found that an adequate field may be self-induced by building the chamber to contain iron plates as shown in figure 1. With the exception of the actual arc tips it is necessary to keep the arc from striking any metal parts in the arc chamber. Unless this is done the volatile metal resulting from contact with the arc interferes with the deionizing action; and the display of molten metal is objectionable. To obtain this result the iron plates used for intensifying the magnetic field to create the blast are located directly above and in the same plane with the insulating plates so as to be definitely out of the arc path.

The interrupting chamber built in this way limits the arc length to a definite value. During d-c interruption, the field is strongest when the current is high at the beginning of the interruption. Consequently, both the deionizing action produced by the magnetic-blast effect



(a) Closed position



(b) Open position

Figure 2. Circuit-breaker contact construction

of the field and the ionizing action produced by the heavy current in the arc are initially high. As the current decreases, both ionizing and deionizing forces subside and the voltage gradient of the arc remains substantially constant. Since the length is limited and constant when the full value is reached, the arc drop as a function of time approximates the desired curve shape. There is no high overvoltage at the instant of interruption.

During a-c interruption, an early current zero is forced, but the arc drop is not high until the end of the arcing half cycle because of the normally sinusoidal current and its effect on the field. There is only one half-cycle of arcing except when the

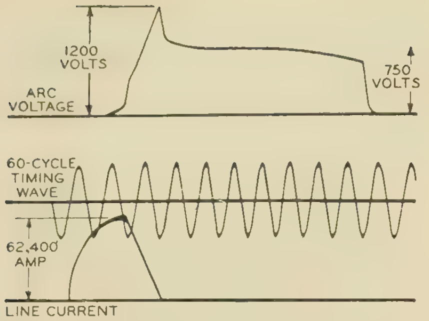


Figure 3. Oscillogram showing interruption of 62,400 amperes at 750 volts direct current

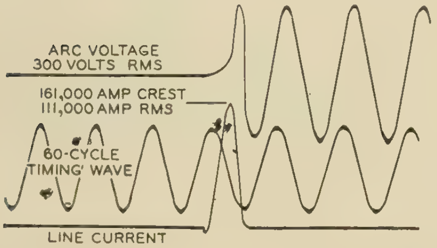


Figure 4. Oscillogram showing single-phase interruption at 111,000 amperes, 300 volts, 60 cycles

contacts part just prior to current zero, in which case the time will be slightly longer.

The arc is well enclosed by the chamber, and the gases which pass between the plates and out the top of the chamber are deionized by the plates so that very little flame passes out of the top. Breakers equipped with these interrupters have been tested in enclosures with the top surface only one inch above the arc chamber, and interruption was satisfactory. The chamber is effective as a muffler, and noise is greatly reduced by its use.

The voltage drop of the arc in the chamber can be as much as 350 volts per inch direct current, and 550 volts per inch root-mean-square, alternating current. Currents as high as 120,000 amperes root-mean-square have been interrupted satisfactorily.

III. Air Breaker Construction

An air breaker designed to handle considerable current must incorporate a plurality of contact pairs in order to isolate properly the functions of carrying load current and interrupting short-circuit current. If high short-circuit currents are to be interrupted, it is necessary to use three sets of contacts. The first set, or main contacts, serves only to carry load current. In order to make the over-all dimensions of an air breaker as small as possible for a given current rating, it is necessary to use materials and constructions for the main contacts which will carry the load currents with as little ohmic loss as possible.

The most satisfactory structure for this purpose has been found to consist of an upper and lower main-contact stud constructed of copper with a solid copper bridging member, which breaks contact when the breaker is opened. The effectiveness of the contacts is greatly increased by the use of silver plates brazed on to the contact faces of both moving and stationary members. The silver should be in the form of a thick plate rather than thin electroplating, in order to provide long life and freedom of maintenance. Sufficient spring pressure must be used to insure a low contact drop with normal load current. It is impractical and unnecessary to utilize spring pressures so high that the main contacts will not be blown apart by heavy short-circuit currents, because in the event of short circuit these contacts must part as the breaker opens; and other parallel contacts must, therefore, be so designed that the main contacts will be well protected.

High-pressure silver line contacts are superior to low-pressure silver surface contacts. As shown in figure 2, the line contact is obtained by machining the face of one silver plate to have the profile of a segment of a circle. Proper alignment of the contacts is necessary to ensure good line contact. This has been achieved by placing the silver at a 45-degree angle on the main contact studs and machining the bridging member so that the contact lines lie on a cylindrical surface. The cylinder will properly align itself with the two planes formed by the stationary stud contact members as the bridge member is somewhat free to slide into proper position.

This main contact construction has proved so effective that the over-all dimensions of the new breaker which will handle 1,600 amperes are no larger than breakers of earlier form, using laminated brush construction, which could carry only 800 amperes with the same temperature rise.

In order to protect the main contacts from being burned as the breaker passes and interrupts heavy short-circuit currents, it is necessary to use a set of protective contacts and a separate set of arcing contacts, as shown in figure 2. The contacts must obviously separate in proper sequence, that is the main contacts part first, then the secondary or protective contacts, and finally the arcing contacts. In order to provide for the proper mechanical sequence of operation it has been usual practice in air-circuit-breaker designs either to make the contact-carrying arm a flexible member or to provide in it a pivoted joint and springs mounted to hold the contacts closed. The flexible-arm construction is not satisfactory since it is too readily bent by magnetic forces associated with extreme short-circuit currents, with the result that the arcing and protective contacts may open before the main contacts. A rigid contact arm, when provided with a separately moving member which carries the arcing contacts and the necessary springs, is difficult to design in a neat manner and also has the disadvantage that its weight and inertia are considerable, which results in a decreased mechanical speed of breaker opening. As shown in figure 2, the necessary relative motion of the secondary and arcing contacts has been provided by a movable platform member on a stationary-contact side of the breaker. As the breaker opens, this platform member moves directly with the contact-supporting arm in such a way that the secondary and arcing contacts do not slide on each other or separate. The necessary freedom for the moving platform member is provided by a slot in the side plates which support it. After a sufficient degree of motion has taken place to separate completely the main contacts, the pin which is a part of the platform member reaches the end of the slot in the side plates and the secondary contacts part. With further breaker

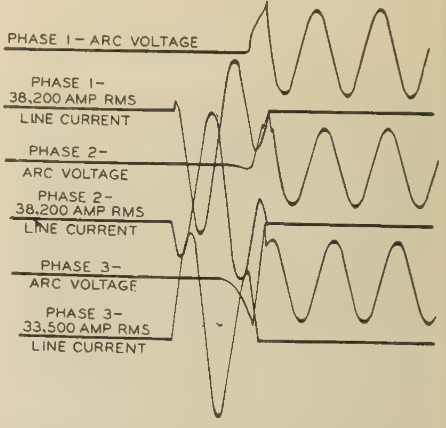


Figure 5. Oscillogram showing interruption of a three-phase short circuit at 600 volts, 60 cycle

motion the platform member pivots about this pin, the secondary contacts on the stationary side move backward, and the arcing contacts remain closed until the stop at the upper portion of the platform member reaches its final position. At this point the arcing contacts separate.

By means of this construction it has been possible completely to enclose the springs and shunts and to provide a simple design unusually neat in appearance.

The secondary-contact material must be chosen to resist some arcing, but at the same time the contact drop must be kept low in order to provide necessary protection for the main contacts. A tungsten alloy has been used. The arcing contacts must be very resistant to damage from the arc, and a similar tungsten containing a higher percentage of tungsten has been used. This material is much superior to carbon since the contacts may be made smaller, more free from breakage, and have better thermal capacity and conductivity.

It is important to use adequate shunts for connection between the secondary and main contacts in order to transfer the current from the main contacts to the secondaries without burning the mains. The shunt, contained within the moving platform member, is inverse in form, so that the heavy short-circuit current will tend to close the secondary contacts tightly together. The current path to the arcing contacts cannot be constructed in this way since the magnetic field must be such as to blow the arc upward from the arcing contacts when it

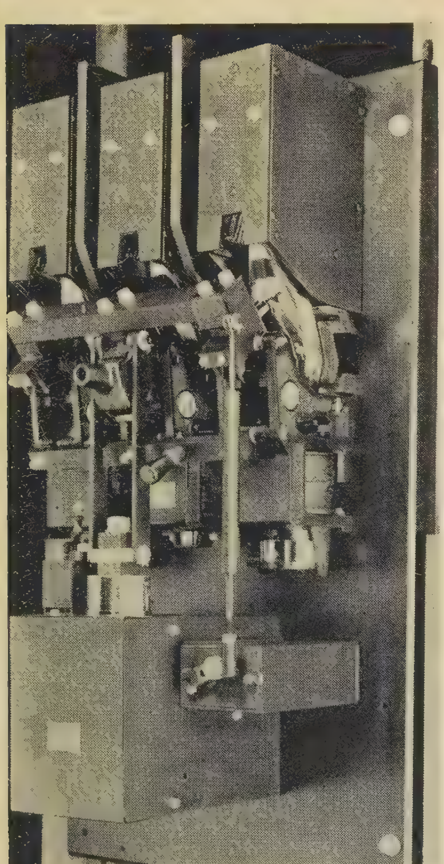


Figure 6. A complete electrically operated circuit breaker

is formed. Consequently, the spring pressure has been made adequate to keep the arcing contacts together under sufficient pressure, even against the effect of very high short-circuit currents.

To protect completely the main contacts from the effect of the arc between the upper contacts, a pair of horizontal overlapping baffles were inserted directly below the secondary contacts. Any gases moving downward are consequently deflected and there is no danger of the arc striking between the mains when interrupting heavy currents.

It is pointed out in section II that high mechanical speed of opening is necessary to minimize the energy which must be dissipated in the interrupter. This has been achieved in the breaker by making the moving contact arm and its associated members as light as possible and by using heavy springs to accelerate the breaker initially. With short-circuit currents in the higher brackets complete operation, which includes tripping of the breaker, mechanical opening, and arc extinction, takes place in a single half cycle. Even in the case of lower short-circuit currents, the operating speed is unusually fast and complete operation does not ordinarily exceed two cycles.

To withstand the forces imposed upon the structure as the result of extreme short-circuit current and the high opening speed, an air dashpot shock absorber was designed to absorb the kinetic energy of the moving-contact arm. Without a device of this kind rebound was noted. The dashpot, however, is so effective that rebound is eliminated. At the same time the stresses are greatly reduced, so that no part is in danger of mechanical breakage.

IV. Interrupting Tests

To obtain the necessary range in current at 600 to 750 volts direct current, test apparatus consisting of four 1,500-kw generators was used. D-c tests were made on single-pole breakers with currents ranging from only a few amperes up to and including 62,000 amperes (table II).

Both single-phase and three-phase short-circuit tests were made at 440 and 600 volts, 60 cycles, using a test set consisting of a three-phase 3,000-kva bank of low reactance transformers. These tests ranged in current values from a few amperes up to 120,000 amperes root-mean-square with crest values over 200,000 amperes. However, in order to obtain currents above 45,000 amperes the low-voltage windings of the transformers were connected in parallel, causing a reduction in voltage to approximately 300 volts. Since all tests above 45,000 amperes were made single phase with 300 volts across the pole, they were equivalent in effect to similar three-phase tests at 520 volts (table I).

Table I. A-C Tests

Test Number	Volts, 60 Cycles	Kind of Test	Actual Current Measured by Oscillograph: Root Mean Square		Actual Current Measured by Oscillograph: Crest
Single phase					
1	575	O	4,750		7,900
2	575	O	7,900		11,200
3	575	O	22,300		34,000
4	575	O	32,500		52,000
5	288	O	46,000		72,000
6	288	O	65,000		92,000
7	288	O	76,000		112,500
8	288	O	111,000		161,000
9	288	O	124,000		204,000
Three phase			Phase 1	Phase 2	Phase 3
10	600	O	34,400	42,200	36,600
11	600	O	38,200	38,200	33,500
12	600	CO	13,800	14,800	17,700
13	600	CO	28,800	30,200	24,000

Table II. D-C Tests

Test Number	Volts, 60 Cycles	Kind of Test	Actual Current Measured by Oscillograph
14	750	O	20,000
15	750	O	36,900
16	750	O	62,400

The oscillograms, figures 3, 4, and 5, show how rapidly and smoothly the arc voltage increases and the effective arc-extinguishing action obtained by this means. The arcing time is definitely limited to one-half cycle or less and this coupled with fast breaker action provides an over-all operating time varying from three cycles at low short circuits to one-half cycle at very heavy currents.

The use of the new deionizing interrupting chamber greatly reduces the amount of noise and disturbance which ordinarily takes place when heavy short circuits are opened by air breakers. The ionized flame which ordinarily accompanies interruption was also greatly reduced, and interrupting tests with these breakers in metal cubicles of dimensions very little larger than that of the breaker itself indicated no danger of flashover between phases or to ground.

V. Conclusions

Utilizing the new form of arc-interrupting device in conjunction with an air

circuit breaker improved both electrically and mechanically in fundamental respects, has made it possible to build an air circuit breaker of comparatively small size which will interrupt short-circuit currents up to 120,000 root-mean-square on alternating current, or 62,000 amperes on direct current. The complete breaker electrically operated is shown in figure 6. The noise and demonstration which are usual in air-circuit-breaker practice have been greatly reduced. The ionized flame has also been reduced to an extent that these breakers can be compactly mounted in steel cubicles without the necessity of providing unusual barrier arrangements or dead space above the breaker for taking care of the ionized gases.

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Discussion

Charles P. West (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Messrs. Ludwig and Grissinger have described the latest advance in the circuit-interrupting art. It is another step in the series of breaker improvements founded on Doctor Slepian's fundamental concepts of arc control. The commercial developments arising from these early theories are affecting not only arc-interrupting devices, but the supporting switchboards, structures, housings, and other associated gear as well

This new breaker design makes it possible to meet the ever increasing demands for reduction in the size of structures. The ability to carry and interrupt current in a given space has been greatly increased. For instance, it is now possible to house three manual three-pole 600-volt 1,600-ampere 60-cycle 40,000-ampere-interrupting-capacity metal-enclosed drawout circuit-breaker units in a space 26 inches wide and 90 inches high. A three-pole drawout unit for 2,000 amperes is shown in figure 1 of this discussion. It requires a width of 30 inches and height of 45 inches. Figure 2 shows the rear of this truck. Note the liberal design of the primary contacts. The safety interlocking features common in metal-clad gear are provided. Three-thousand ampere units are the same width and 60 inches high. Such compactness is now possible because so little clearance over the arc box is necessary and the new breaker frame is much smaller.

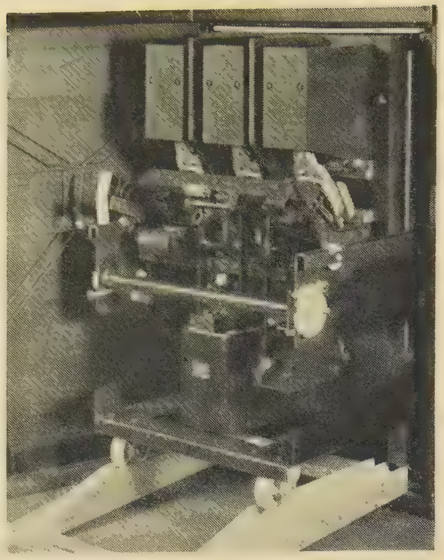

Figure 1

Figure 3 shows a structure for 2 600-ampere, 12 1,600-ampere manual, and 4 2,000-ampere electrically operated units. Semiflush instruments and relays for two generators, two transformer banks, a bus tie breaker, and 16 feeders are provided. Two three-phase reactors are mounted in the superstructure. Housing these high-current-capacity drawout breakers in limited spaces requires liberal ventilation. This feature must be carefully watched in the design of the structure and bus compartments. Another factor demanding attention is the locating of the copper for the bus and connecting circuits. As the breaker units get smaller, the width per circuit decreases and it is more difficult to provide accessibility and the required electrical clearances.

Permissible temperature rise in apparatus is basically determined by the top operating temperatures which the materials used can stand continuously without damage. Different classifications are recognized and maximum temperatures determined by the design features of each. As materials and finishes are improved, apparatus can be safely worked at higher temperatures. For instance, bus temperatures are limited so that no trouble will occur in joints. Silver plating eliminates harmful oxides and higher temperatures might well be permitted. Air breakers of the type described by Messrs. Ludwig and Grissinger employ silver-faced solid contacts. Operating temperatures for such a design are less than 30 degrees centigrade and need never be limited to the 20-degrees-centigrade rise often thought desirable for laminated brush breakers. In fact, no serious or permanent damage would result from emergency operation considerably above their rating.

As the performance improves, the size decreases and safety factors are increased, air breakers are finding wider application in fields such as central-station auxiliary service. Here the utmost in reliability is demanded. Low-voltage metal-clad switchgear, as illustrated, provides the safety, interchangeability, reliability, compactness, appearance, and trouble-free operation which are essential to the perfection in design for which central-station engineers are striving.

These various points are discussed to emphasize that, as breaker design progresses, the associated gear keeps pace with it so that the full benefits are made available to industry.

E. A. Childerhose (Jackson and Moreland, Boston, Mass.): The manufacturer is to be congratulated on having taken another successful, even if tardy, step in the development of air circuit breakers. The advantages of air circuit breakers over oil circuit breakers are so numerous that the latter (all voltages) should be obsolete within the next ten years, with the possible exception of special applications in mines and similar locations where the explosion hazard exists and the use of any open arc is prohibitive.

The designers of this new type of breaker have done a commendable job in reducing the space requirements, and have thereby earned the blessings of many a designer, harassed by limited space in which to locate his switchgear. They have incorporated features found in some older breakers, discarded some of the old features, and developed new ones. They have succeeded in developing a snappy, high-speed mechanism that is in many ways superior to the old-style breaker which it supplants. In doing so they have undoubtedly had to compromise between the best and something that is good enough to do the work in order to accomplish the major features desired in the new breaker. These, and the absence of features familiar to the operating man in the old style breakers, naturally raise questions in his mind as to why they were done or omitted. He knew the limitations of the old equipment, and now wants to know what the limiting features of the new will be.

Gone is the familiar brush contact and in its place a silver line contact. Instead of a multitude of small surface contacts is a single line contact. A little bit of arcing

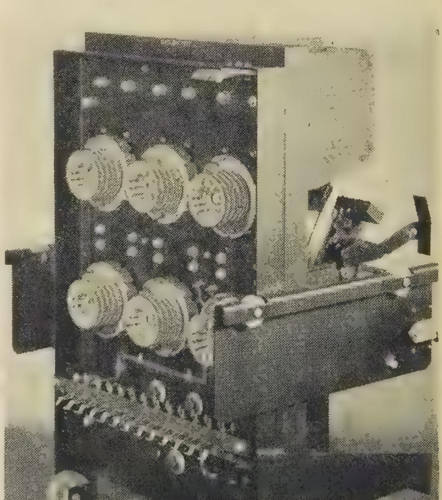

Figure 2

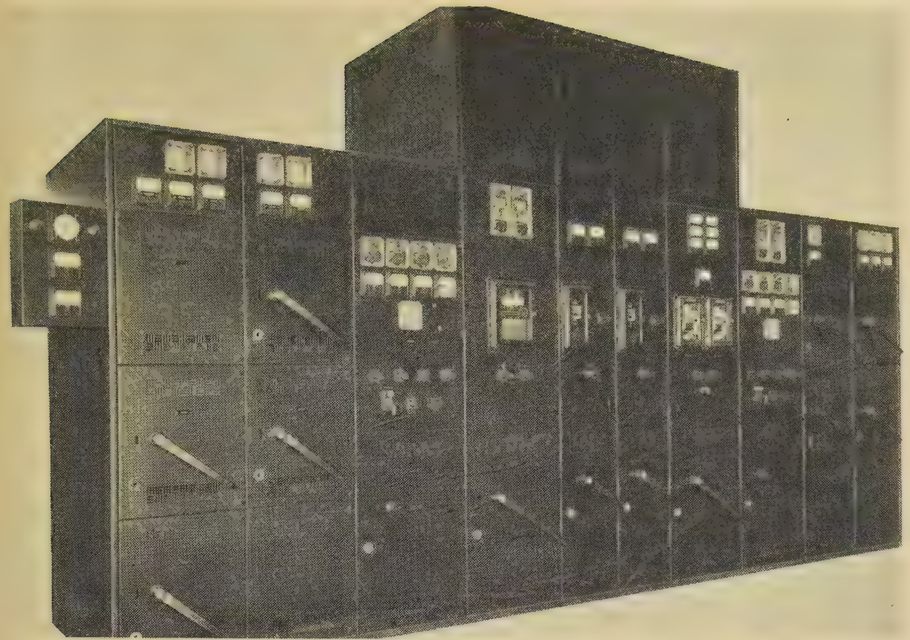

Figure 3

might render a couple leaves of the old brush ineffective, but it reduces the new contact from a line to two points, or possibly even only one! What effect has this on the current-carrying capacity? Will it not be materially reduced, and will there not be an increase in the probability of overheating when the breaker is carrying rated, or even normal operating loads? Silver has a lower melting point than copper and is an excellent solder. There appears to be considerably more danger of a slightly pitted line type of silver contact freezing under moderate overloads, or when opening small-magnitude short circuits, than there is with the brush type of contact.

The spring pressure on the main contacts is not sufficient to prevent them opening under heavy short circuits. The current then travels through the shunt around the bottom contact, and the secondary and arcing contacts. Is the reactance of these shunt circuits sufficiently low to prevent arcing across the main contacts? It is again asked, "What will be the effect of arcing on these silver line contacts?" This is an important consideration when the breaker is adjusted for delayed opening, though because of high operating speeds it may not matter when the breaker is set for instantaneous opening.

The contact pressure of the single line contact has been increased over that used in the brush in order to make it carry rated load. How will it compare when carrying overloads? It is questionable that it has as great an overload capacity.

The authors show oscillograms of the breaker opening 124,000 amperes on an "O" test. How does this compare with the standard "two CO" test for oil circuit breakers? What would the comparative interrupting capacity be? The "two CO" test is such a common reference that many are apt to assume that the currents reported are on that basis, though the authors have made no attempt to intimate such a rating. In what conditions were the contacts after breaking these high currents? Was the breaker operable and capable of carrying rated load after the tests? What was the rating of the breaker tested? Is it to be assumed that all sizes of this new breaker are capable of interrupting this amount of current?

When you buy a car you can test it on the open road and determine the utmost it can produce in speed. When you buy a breaker the manufacturer tells you what the interrupting capacity is, but you are unable to test it for yourself. When you are told that these new breakers can interrupt 124,000 amperes, is that all that they can interrupt, or is there considerable margin of safety in that figure? When you use steel, or wood, or concrete in a structure you know their strength fairly accurately, and can apply safety factors, depending upon the use being made of the material. Why cannot similar information regarding circuit breakers be obtained from the manufacturers, and the purchasing engineer determine the safety factor which should be used for any particular installation?

The paper states that these breakers may be used in cubicles of dimensions very little larger than that of the breaker itself. This brings up the question of temperature rise in such a small confined space. Little is known of this feature to the operating man or to the station designer, and less has been published regarding it. A frank discussion by the manufacturers, and dissemination of their test data on temperature rises in enclosed metal-clad switchboards would be of interest to many. More knowledge of the data used in the recently increased temperature rises permitted by the National Electrical Manufacturers Association would be welcome.

It is acknowledged that silver contacts may be safely operated in excess of 70 degrees centigrade, the limit for copper, but that will increase the ambient temperatures within switch cubicles. Will the higher ambient endanger insulation, will it injure instrument transformers and other equipment which present standards permit being operated at their safe maximum operating temperature in a 40-degree centigrade ambient? It may be that we are not taking full advantage of the possibilities afforded by silver contacts.

This discussion has resolved itself into a series of questions. But they are all questions that a prospective user of new equipment wants answered before he decides to purchase. It is hoped that Messrs. Ludwig and Grissinger will see their way clear to answer them.

The paper brings out another point that is of poignant interest to engineers and the country as a whole, namely, the increasing use of silver in industry. Is it not time that the artificial price of silver were abolished and the metal allowed to assume its true position in industry and the life of the country?

J. W. Seaman (General Electric Company, Philadelphia, Pa.): It is with considerable pleasure that I take this opportunity to commend Messrs. Ludwig and Grissinger on an able presentation of a subject which is of considerable interest, particularly so, to those of us who have been engaged in the pursuit of similar interests for the past several years.

The trend toward metal switchgear has long been recognized by those of us engaged in the art, and I believe I can justifiably say, has been considerably furthered by the work my associates have done during the past several years, although we have not especially publicized the results obtained.

Requirements

To design and build air circuit breakers suitable for metal-clad gear several requirements must be met as borne out by Messrs. Ludwig and Grissinger's presentation.

The first of these is a reduction of size. The use of solid-silver line contacts has been an important factor in accomplishing this, as the authors have pointed out. I should be the last to belittle the undoubted advantages of this construction inasmuch as it has been standard in our designs for some *eight years*. The use of silver alloys for arcing tips is another step in this direction and is now, I believe, quite well recognized.

We engineers would be remiss in our duties if we allowed the reduction in physical size, gained largely by the adoption of a solid-silver contact construction, to be penalized by using (for interrupting means) the conventional plain-break construction. Such means, as we know, demand a prohibitive air space over the contacts when enclosed, in order not to impair the current-rupturing ability.

The necessity for some suitable arc-controlling means has, therefore, long been recognized. If such means is properly engineered, it will (as we have demonstrated through several years of practical application) provide other advantages far greater than the mere saving of space.

Interrupters

Messrs. Ludwig and Grissinger have described a particular arc-controlling means. Happily, as borne out by their presentation, the field in this direction is not particularly limited. There are other equally advantageous means. The one with which I am most familiar is known as the pin-type arc quencher.

The type of interrupter described by Messrs. Ludwig and Grissinger functions it seems, due to what we may call a resistance

characteristic, that is, the arc resistance is increased as a result of decreasing the diameter of the arc core as it travels into the V-shaped slots. To obtain successful operation, Messrs. Ludwig and Grissinger have recognized the necessity of obtaining venting in order to reduce the possibility of developing back pressure sufficient to keep the arc from entering the slots. If the arc is not allowed to enter the restricted slots, this device would apparently become totally ineffective. Apparently the size of the enclosing housing may, therefore, have an important effect upon the performance of this device. The magnetic-blast theory attributed to this interrupter we will leave in the capable hands of Messrs. Ludwig and Grissinger. I believe it sufficient to say that the evidence presented indicates successful operation within the apparent limits of the testing facilities.

The arc-controlling means with which I am particularly familiar is an arc-chute structure comprising a multiplicity of pins through which the arc is driven by magnetic means. This device functions due to the efficient presentation of the arc to large cooling surfaces in combination with a multiplicity of cathode drops. That it has no particular voltage or current limitations has been demonstrated by numerous tests. The arcing time averages less than one-half cycle over current ranges up to and including 125,000 root-mean-square amperes at 600 volts, both single phase and three phase. Due to its freedom from restriction troubles it has been successfully applied over the entire range of air-circuit-breaker sizes during the past three years.

With these test data and operating experience available, we are naturally gratified to find that Messrs. Ludwig and Grissinger concur with us in recognizing the necessity for suitable arc-controlling means, to greatly reduce the usual disturbance associated with the opening of arcs in air, and permit retaining the space advantages already gained by the improvements in contact structure construction.

There is another phase involved in satisfactorily meeting this trend toward metal-clad gear with modern air circuit breakers. This is perhaps not as well appreciated as the phases we have just been discussing but is rapidly assuming greater importance. The increasing demand for electrically operated devices reveals that most present-day air circuit breakers require considerable additional space in order to obtain electrical operation. In the line of air circuit breakers with which I am most familiar, this problem is being met by designs which inherently require only the same small space for electrical as for hand operation. The reception accorded these new breakers clearly indicates that this last trend is not to be lightly ignored.

In concluding, let me again congratulate Messrs. Ludwig and Grissinger on the introduction of their new breaker. With the awakening of new interest even greater improvements in the art, I am sure can be made in the future.

G. G. Grissinger: Mr. Seaman, in his discussion, indicates that the size of the enclosure may have an important effect on the interrupting performance of this device. Recognizing that a limitation of this sort might exist, very thorough tests were made using an experimental enclosure having an adjustable top which could be raised or lowered so as to vary the distance between the top of the breaker and the top of the enclosure. These tests proved conclusively that breakers of this type may be very closely confined without impairing their interrupting ability, since with the top of the enclosure only one inch above the top of the breaker, three-phase short circuits both "O" and "CO" as high as 97,000 amperes root-mean-square were interrupted satisfactorily.

Mr. Childerhose submits a number of very interesting comments. Most of the questions raised can be answered best by reference to test results.

Theoretically, it would appear that a laminated copper brush with its multitude of point contacts would offer much less resistance to the flow of current and, therefore, be more efficient than a solid butt contact. The solid contact, however, can be made much shorter than a brush and with adequate spring pressure behind them the solid silver faces provide a very effective contact-making means. Although the millivolt drop across the contacts may be slightly higher than that of a corresponding brush at the same current, the gain in reducing the length of the current path more than makes up for the difference. Furthermore, a much more durable silver surface can be applied to the faces of solid contacts.

Mr. Childerhose raises a question concerning the effectiveness of the solid contacts after a heavy or a moderate current interruption. Practically no pitting of the silver contacts occurs except when interrupting very heavy short-circuit currents. Temperature tests on these breakers after a series of interrupting tests ranging from small currents to over 100,000 amperes had been made, showed only a few degrees increased temperature rise, even though the breaker had received no maintenance whatever. Consequently, it is apparent that the slight pitting which takes place when interrupting heavy short-circuit currents has very little effect on the current carrying capacity of the breaker. In no case did "freezing" of the silver contacts occur.

As Mr. Childerhose points out, a number of factors influence the protection received by the main contacts. Among them are spring pressure, resistance and reactance of the shunt circuits, and the conductivity and design of the protective contacts. Consequently, all of these factors were given careful consideration in this development. As a check on the final arrangement chosen, additional short-circuit tests were made in which the circuit breaker was blocked so as to prevent tripping. These are ordinarily referred to as short time or five-second tests. The current after reaching a steady-state condition, measured approximately 42,000 root-mean-square amperes and the breaker on which the test was made was rated at 1,600 amperes. This breaker also carried rated current after these tests, with no maintenance, at a temperature rise only three degrees higher than that previous to the tests.

Due to the relatively low thermal capacity as compared with large rotating machines or transformers, circuit breakers are maximum-rated devices, that is, it is not intended they carry more current than their rating for an appreciable length of time. However, the greater durability of the solid contact at higher temperatures makes it eminently better suited to carrying continued overloads than the laminated copper brush which would be severely damaged by excessive temperatures.

The standard interrupting duty cycle for air breakers, as defined by AIEE Standards No. 20, comprises an "O" followed at a two-minute interval by a "CO" test. The 124,000-ampere test referred to by Mr. Childerhose was made on a 3,000-ampere a-c breaker and was an "O" test only. However, many tests have been made both "O" and "CO," particularly on the smaller frame sizes and while the short-circuit currents on these did not exceed 97,000 amperes, three phase, this value nevertheless represents a considerable factor of safety over NEMA standards, which specify 40,000 amperes for a 1,600-ampere breaker and 60,000 amperes for a 2,000-ampere breaker.

In all cases, after completion of the tests, the breakers were in condition to carry rated current without maintenance, although of course the temperature rises were two or three degrees higher than that of a new breaker.

High-Power "De-ion" Air Circuit Breaker for Central-Station Service

R. C. DICKINSON
ASSOCIATE AIEE

Synopsis: The "De-ion" principle of arc interruption in air has been applied over a wide range of a-c services. Difficult switching problems have been met by them. A new breaker has been developed and tested in excess of 37,000 amperes for the 15-kv powerhouse class. It may be supplied for masonry or steel-cell mounting or as part of complete metal-clad switching equipment.

De-ION air circuit breakers have met some outstanding problems in moderate-voltage switchgear in the past eight years. Since this type gives highly satisfactory operation under severe duty conditions without oil, water, compressed air, or other arc-extinguishing fluid, it is only natural that pressure has been brought by leading users for the extension of this line in the powerhouse field. The purpose of this paper is to describe an important addition to the line tested in excess of 37,000 amperes for the 15-kv class and providing continuous carrying capacities up to 4,000 amperes.

To recapitulate some important services now being performed by De-ion air circuit breakers:

They are giving consistently reliable protection against short circuits up to 500,000 kva at voltages from 12,000 to 16,500, in well-distributed locations throughout the country.

They are handling with a minimum of maintenance, reversing motor service in steel mills in which annual individual breaker operations in excess of 50,000 have been recorded.

Equipped with a special high-speed tripping arrangement, they are opening difficult short circuits on rectifier installations at 12 kv with an over-all breaker time of approximately three cycles (60-cycle wave) and have eliminated potential breakdown such as occurred previously under less effective protection.

In one application they are installed against short-circuit capacities of 1,500,000 kva at 24,000 volts, and giving satisfactory service.

In another installation they are handling satisfactorily contact line circuits in 11,000 volt, 25-cycle, single-phase railway service in which short circuits up to 50,000 amperes are possible and these faults are cleared from the system in less than one cycle.

In view of the gratifying performance of breakers of this type in 15-kv powerhouse service up to 500,000 kva, as exemplified in the hundreds of units in use, a large number of which have been in service for several years, development directed toward extension of the fundamental principles to higher interrupting ratings for powerhouse duty followed as a logical sequence. Such extension involved a new conception of contact arrangement not only to provide the higher continuous-current capacity, but to incorporate as well the ability to close in air upon fault currents up to 100,000 amperes crest, to carry these currents until called upon by relay action to interrupt and, finally, to transfer the heavy currents involved to the deionizing chamber without damage to the main or auxiliary contact members. Development into new ground to obtain higher interrupting capacity was necessary, involving means for moving the arc promptly from the contacts to the deionizing chamber, introducing voltage into the arc in steadily increasing steps and assuring speedy, positive interruption. The results of this development are incorporated in a design, set forth in this paper, capable of greater interrupting duty than any circuit breaker available today not requiring liquid dielectric or other form of stored interrupting medium.

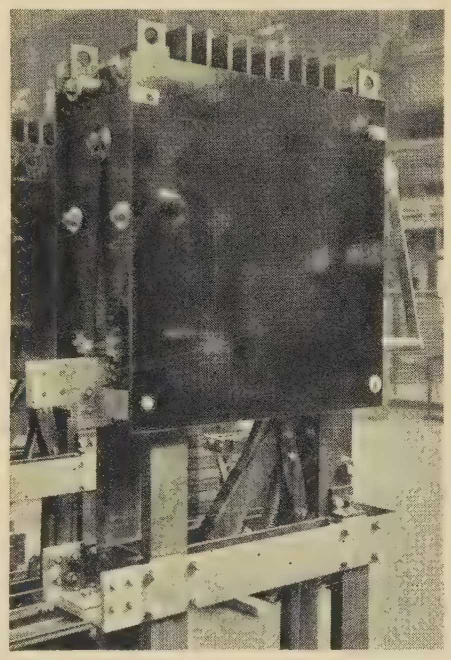

Figure 1. Pole unit of new De-ion air circuit breaker for generation-station service

Description

Figure 1 shows a completely assembled pole unit with a continuous current rating of 2,000 amperes, placed in service last year. Three thousand-ampere breakers are now being installed and a 4,000-ampere design is available for applications that may require it. All of these current ratings have the same general form except for the current-carrying members. General features of the breaker follow closely the vertical arrangement of elements proved successful by several years' experience with smaller breakers. Mounted on insulated posts of high mechanical strength, the contact linkage is operated from below through an insulating rod pushing upward to close the contacts, with the deionizing chamber mounted at the top of the structure immediately above the parting contact surfaces. The particular structure shown permits a wide range of line connections. Inherently, the breaker is adapted to rear connection, entering either horizontally or vertically, but an additional terminal is supplied to permit one vertical front connection in place of either of the other two. All terminals are arranged for standard bus-bar connections.

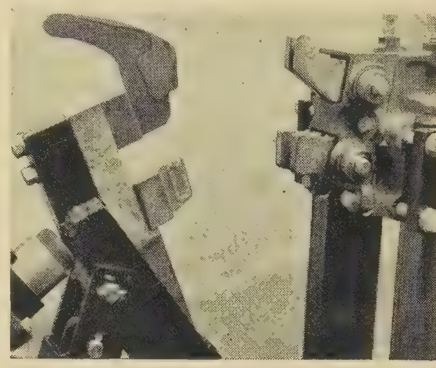

Figure 2. Finger contacts are a radical departure from conventional air-circuit-breaker design. Peak currents in excess of 100,000 amperes were closed with this type of contact during "CO" tests

Paper number 39-35, recommended by the AIEE committee on protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 25, 1938; made available for preprinting December 29, 1938.

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Table I. Single-Pole Interrupting Tests With Separate Closing Breaker, 15-Kv De-ion Air Circuit Breaker

Test Number	Root-Mean-Square Amperes Interrupted	Breaker Time	Root-Mean-Square Volts Restored
1	4,600	4.2	12,700
2	20,500	4.0	11,800
3	25,000	3.8	11,700
4	30,000	3.9	11,800
5	30,400	3.9	12,000
6	32,200	4.1	11,800
7	32,200	3.8	11,600
8	34,000	4.1	12,000
9	34,600	3.9	11,600
10	41,500	3.8	12,200

As indicated by figure 2, the contact elements of this breaker depart from previous air-circuit-breaker design to permit extending their function as outlined previously. The finger type of contact here shown carries all the advantage accruing from their use in conventional breakers together with the incorporation of features particularly adapted to air-breaker construction. These contact fingers are arranged in two individual pairs, each with its own entering member. The lower set comprises the main current path while the upper set is designed for arc-drawing purposes. Both pairs of fingers consist essentially of parallel bars so arranged as to permit of ample flexible shunts being extended to the line terminal in a straight line thus avoiding local loops with their accompanying current forces in the current path. The fingers are biased toward each other by heavy compression springs with a definite stop maintaining them at proper separation for entry of the moving member. In addition to the self-adjustment permitted by the flexible shunts, the fingers are in effect pivoted in the contact-supporting casting so as to be further self-aligning within the limits necessary to prevent stubbing of contacts upon closing. The main fingers are wide and rugged with sufficient overlap of the entering member to provide the liberal contact surface necessary for high continuous and momentary current duty. The mechanical and thermal sufficiency of this design was well proved by numerous tests in which the fault currents were as high as 104,000 amperes.

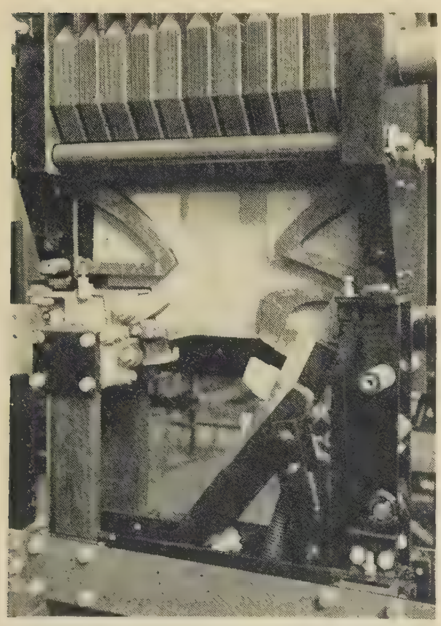

Figure 3. The shape of the arcing contact finger assists in movement of the arc upward to the arc horn without detracting from the ability to close high currents. The arc horns are spaced laminations for free venting and the contour gives rapid motion of the arc terminals

New features are incorporated in the arc-drawing pair of fingers. The parallel conducting bars forming the fingers are of high-strength high-conductivity alloy specially heat-treated and only recently made available in this form. An important feature of this pair of fingers is the shape of their contact surfaces. In the fully closed position these surfaces are such that the current path through them (in an elevational view) is substantially in a straight line with the parallel bars and shunts. The lower portion of this surface is cut away so that as the contacts open, the point of contact moves upward making the last point of contact well above the body of the fingers and introducing a sharp local loop in the current path at the instant of initiating the arc.

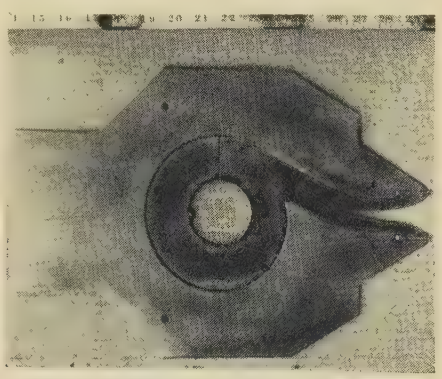

Figure 4. Copper and steel plate combination used in the new breaker

The magnetic force produced by this loop, not present in the fully closed position, assists materially in moving the arc rapidly from its point of origin on the contacts to arcing horns provided for the purpose and reduces to a minimum the burning on the contact surfaces. Both stationary and moving arcing contacts are faced with silver-tungsten, arc-resisting alloy. The moving element, essentially a plate, is formed with a horn to assist the arc in moving upward toward the deionizing chamber, even while the amount of contact separation is still very small. This horn also carries arc-resisting alloy on its upper surface.

This type of contact has several advantages for operation on high currents. Speed of contact separation is an important factor in air-breaker performance. The amount of contact movement afforded by finger-type contacts before actual parting occurs, decreases the accelerating force necessary for a given speed of separation and this decrease in accelerating force also decreases the closing effort, a vital factor in heavy-duty breakers. The finger type of contact also

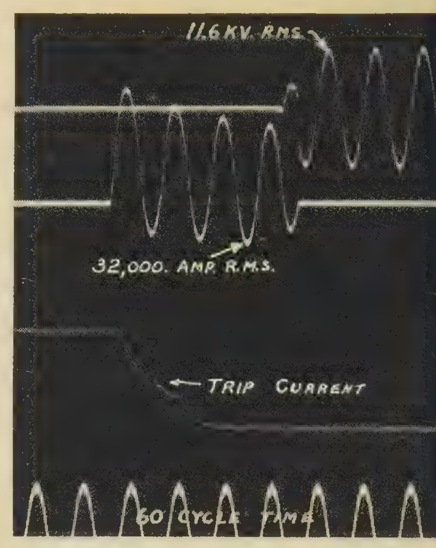

Figure 5. Oscillograms were made of all interrupting tests. The above film shows typical performance at high current values. The short circuit was closed by a separate breaker

has the property of resisting the tendency to be biased toward the open position under short-circuit conditions which again is reflected in decreased closing effort.

The contact-supporting casting is divided into two parts one of which supports both pairs of stationary fingers with their shunts. This contact supporting section may be removed from the main shell of the casting thus permitting maintenance work on the fingers without disassembling other parts of the breaker. As shown by figure 3, a side of the arc box may be removed for a more detailed inspection of this portion of the breaker while the contacts remain in their operating position. It will be noted that the space between the contacts and the lower

end of the deionizing chamber is relatively deep and that the horns provided for arc travel through this space are proportioned to provide a gradual lengthening of the arc, and with it a steadily increasing arc voltage, until it is at the point of entering the slotted plates of the deionizing chamber. These arcing horns are of spaced, laminated construction to provide venting as the arc moves along them.

The copper deionizing plates shown in figure 4 are designed from the point of

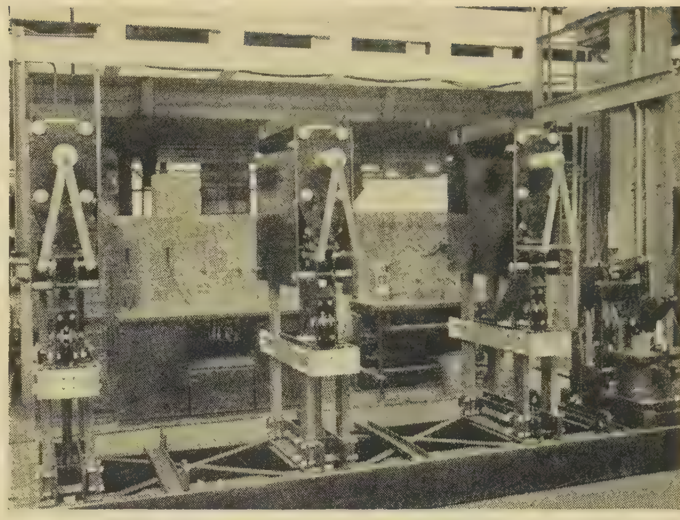

Figure 7. Designed to meet particular spacing requirements, this unit illustrates the adaptability of this breaker to meet special conditions

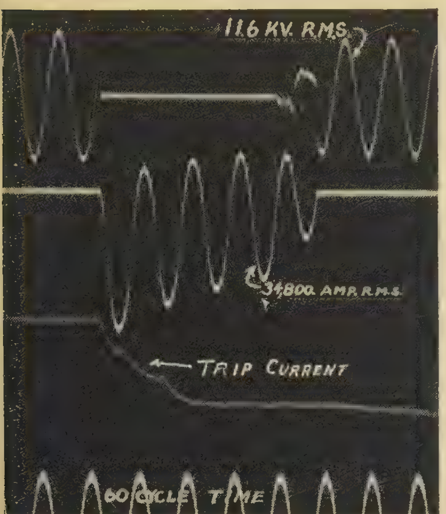

Figure 6. This film shows the performance of the breaker when closed against a peak current of 96,000 amperes and opened on 34,800 amperes

view of continuing the work done as the arc passed upward along the horns. Extended to the full length of the chamber before entering the plates, the arc first encounters the long, gradually tapered entering slot. Passage along this slot tends to contract the core of the arc ("Extinction of an A-C Arc," J. Slepian, AIEE TRANSACTIONS, volume 48, April 1939), further increasing the arc voltage until at the end of the slot it is ready to transfer to the copper plate surfaces and change from a single long arc to a multiplicity of short arcs in series, each with its own cathode and anode drop. The steel plate surrounding each copper plate, also shown in figure 4, is more liberal in design than in previous structures to provide a stronger magnetic field for moving the arc core along this tapered slot onto the copper plates.

Thermal capacity of the deionizing chamber is a vital factor in interrupting heavy currents with the De-ion air circuit breaker since arc energy is transformed into heat in the process of interruption. Compared with earlier construction, two modifications appear in this design providing increased thermal capacity of the chamber: the thickness of the individual copper plate is increased, and the number of turns in the radial field coils is increased to give a higher speed of arc rotation around the annular path. That is, an increased cubic content of copper per gap provided while the time of exposure of the arc to any particular point on the plate is decreased. Improvement in electrostatic shielding produces more uniform distribution of voltage across the series of gaps to obtain a more efficient interrupter.

While these breakers are rated at 15 kv they are designed with 23-kv insulation in line with other powerhouse breaker practice. The 54-kv one-minute 60-cycle hold tests, and the 100-kv, $1^1/_2$x40 impulse wave tests were met without unduly increasing the size of the breaker as determined by its interrupting requirements. This applies to all the conventional tests, that is, across the open breaker as well as from the breaker terminals to ground, without the use of any supplementary disconnecting devices.

Three-pole breakers made up of single-pole units here described resemble, in general, earlier constructions of smaller breakers with the three units mounted on a single frame and operated by a conventional solenoid mechanism mounted at one side or underneath the breaker. Figure 7 shows a shaft-operated three-pole breaker with the closing solenoid at one end of a common frame and is representative of general construction in all except pole spacing which was here determined by special installation requirements. This breaker with its $52^1/_2$-inch pole spacing was a complete factory-assembled unit greatly simplifying installation. Figure 8 is a dimensioned drawing illustrating a more compact design with the same pole units spaced $27^1/_2$ inches apart and with the solenoid mechanism located underneath the breaker, operating through a common shaft. This arrangement is well adapted to metal-enclosed construction permitting the mechanism compartment to be completely isolated and accessible while high-voltage parts of the breaker are energized. Figures 9 and 10 illustrate respectively a steel cubicle lay and a horizontal drawout

Table II. Single-Pole Interrupting Tests, 15-Kv De-ion Air Circuit Breaker—"O" and "CO" Operations

Test Number	Peak Current Closed Against	Root-Mean-Square Current Interrupted	Breaker Time	Operation	Root-Mean-Square Voltage Restored
1		4,400	4.4	O	12,600
2	5,600	4,250	4.4	CO	12,600
3		30,300	4.0	O	11,800
4	92,500	31,800	3.8	CO	11,600
5		30,000	4.0	O	11,800
6	96,000	32,100	4.0	CO	11,600
7		35,000	3.9	O	11,800
8	56,500	28,200	4.0	CO	11,600
9		4,600	4.4	O	12,600
10	10,500	5,000	4.6	CO	12,600
11		4,800	4.4	O	12,600
12		34,500	4.0	O	11,800
13	95,000	35,250	4.0	CO	11,600

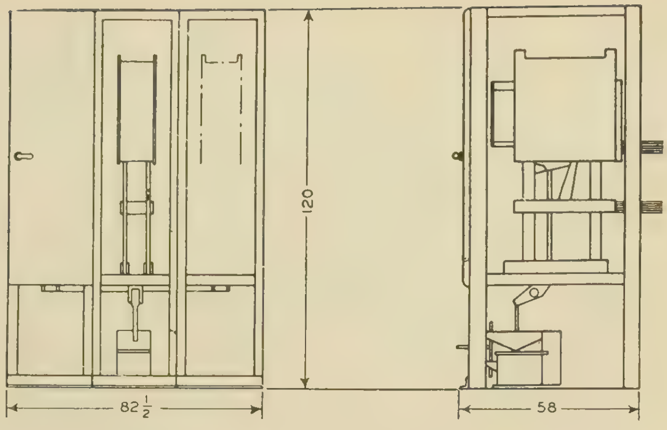

Figure 8. This breaker may be completely enclosed in steel if desired. The result is a comparatively compact assembly

metal-clad unit embodying modern features for this class of equipment.

Tests

In the development of this breaker, the usual insulation tests, operating tests, and current-carrying tests were made. Voltage distribution along the De-ion stack was studied to determine the best type of static shield. And finally, to prove the interrupting ability, several series of tests involving several hundred interruptions, were made, with full generator voltage across a single-pole unit, up to practically the full capacity of the high-power laboratory.

These tests were made on a single-pole unit operated by a conventional four-inch trip-free solenoid having a standard shunt trip magnet. For the sake of simplicity, the first interrupting tests were made without main contacts, the short circuit being initiated by a separate closing breaker. These tests were made with the test generator excited to 13.2 kv, 60 cycles. Figure 5 shows an oscillogram made while this breaker was interrupting 34,000 amperes. Table I shows data taken from typical tests made during this series. Outstanding facts presented by these figures are:

1. The time from energizing of the shunt trip coil to interruption of the circuit is from 3.3 cycles to 4.9 cycles, on a 60-cycle basis for currents from 4,000 amperes upward.

2. The interrupted currents range as high as 41,500 amperes.

3. The restored voltage averages more than 12,000 volts. This is significant in view of the fact that this is impressed on a single-pole single-break unit. Since almost all faults in this type of service involve ground, resulting in considerably less than line voltage being impressed on any one pole, this performance indicates a considerable factor of safety.

Table II shows data taken from a series of 13 single-phase tests made alternatively on the "O" and "CO" basis for customer representatives. These tests were made with the test generator excited to 13.2 kv. Of these tests, seven were at 30,000 amperes or more and the highest current interrupted was 35,250 amperes.

In these tests, the ability to close against high currents was demonstrated. The highest current closed against was 95,000 amperes crest. In other similar tests currents as high as 104,000 amperes crest were successfully closed.

Figure 6 shows a typical oscillogram made on a "CO" unit operation of the breakers.

Tests were then made on the breaker equipped with main contacts, rated at 2,000 amperes, 60 cycles. In these tests the operation was alternatively "O" to "CO." Table III shows results from these tests. The entire series of tests shown were made consecutively and without delay except for precautions to prevent overheating of the deionizing chamber due to the rapid operation at such high currents. By comparison with tables I and II it will be seen that the performance of the breaker under the three conditions is very uniform.

After these tests the breaker was in good operating condition and adequate for further service. The deionizing chamber, though dismantled for inspection, was reassembled and used for additional interrupting tests.

In making the interrupting tests on this breaker, its endurance under severe duty was forcibly demonstrated. In De-ion air-circuit-breaker testing it is quite common to make 20 or more tests without any maintenance on the breaker other than artificial cooling of the deionizing plates, which is required by the rapidity of the tests. For instance in referring to table III, the 21 tests were made in one continuous series. In eight of these tests the interrupted current ranged from 28,400 amperes to 34,800 amperes, six of them being above 30,000 amperes. Of the remaining 13 tests, 7 ranged from 11,800 amperes to 24,800 amperes. The remainder were at currents of from 1,300 amperes to 5,700 amperes. All tests were made with 13.2 kv on the generator. Nine tests were on the "CO" operating cycle and the highest current closed was 96,000 amperes crest. This series of tests was made without any maintenance or parts replacement, except the aforementioned occasional cooling of the deionizing plates. From the users' point of view this performance is impressive, as this one series

Figure 9. Cubicle layout for the new air circuit breaker

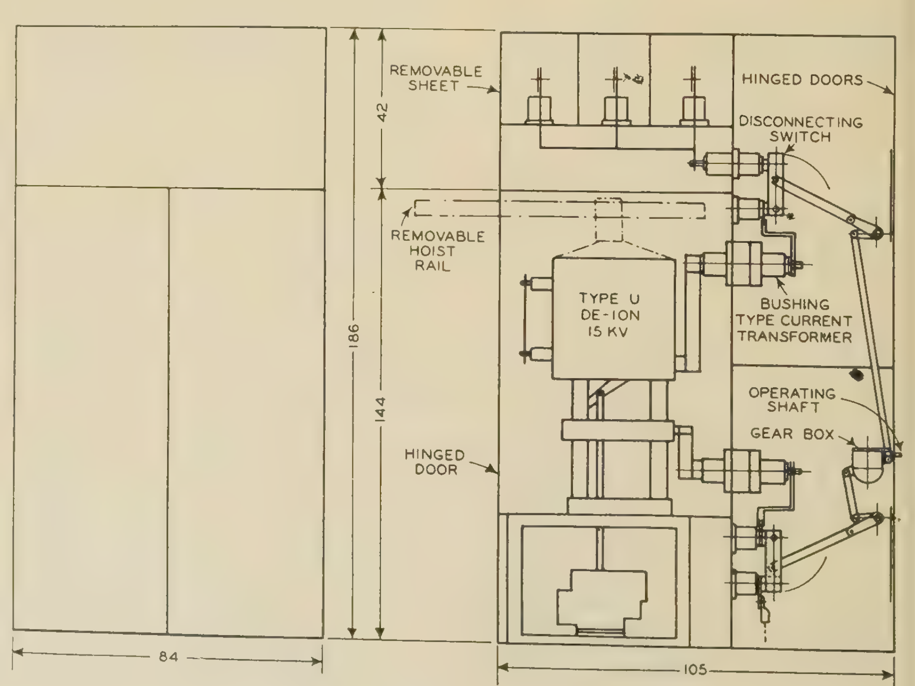

Table III. Single-Pole Interrupting Tests, 15-Kv De-ion Air Circuit Breaker—"O" and "CO" Operations

Test Number	Peak Current Closed Against	Root-Mean-Square Current Interrupted	Breaker Time	Operation	Root-Mean-Square Voltage Restored
1		1,300	5.8	O	12,700
2	2,500	1,400	4.3	CO	12,700
3		3,700	4.6	O	12,700
4	7,000	3,500	4.2	CO	12,700
5		5,700	4.5	O	12,500
6	8,200	4,600	3.6	CO	12,700
7		11,800	4.3	O	12,500
8	27,700	13,300	4.0	CO	12,500
9		13,400	4.3	O	12,700
10		10,200	4.0	O	12,700
11		17,500	4.6	O	11,900
12		24,800	4.0	O	11,900
13	42,500	22,000	4.5	CO	11,900
14		29,000	4.0	O	11,900
15	72,700	31,900	4.1	CO	12,400
16		32,000	4.3	O	11,500
17	49,700	28,400	5.1	CO	11,500
18		32,000	4.3	O	11,500
19	96,000	34,800	4.6	CO	11,600
20		31,300	4.4	O	11,500
21	74,000	31,200	4.3	CO	11,600

of tests is equivalent to many years of service.

Conclusions

Air circuit breakers incorporating fundamental De-ion principles of arc rupture have now been made available to a widening circle of application where, for individual reasons, the air breaker is preferred. Breakers of this type are available for general a-c switching applications at voltages from 2,500 to 15,000, the largest unit in the 15-kv class having been tested beyond 37,000 amperes, with some applications at 24 kv.

The basic De-ion characteristic of a dry breaker has been extended beyond the capacity of any other breaker except those operating in oil or other liquid dielectric. As with earlier forms of De-ion air circuit breakers, this larger breaker lends itself equally well to all forms of modern switchgear construction such as cell, metal enclosed, or truck. It also forms an important element in modernization programs.

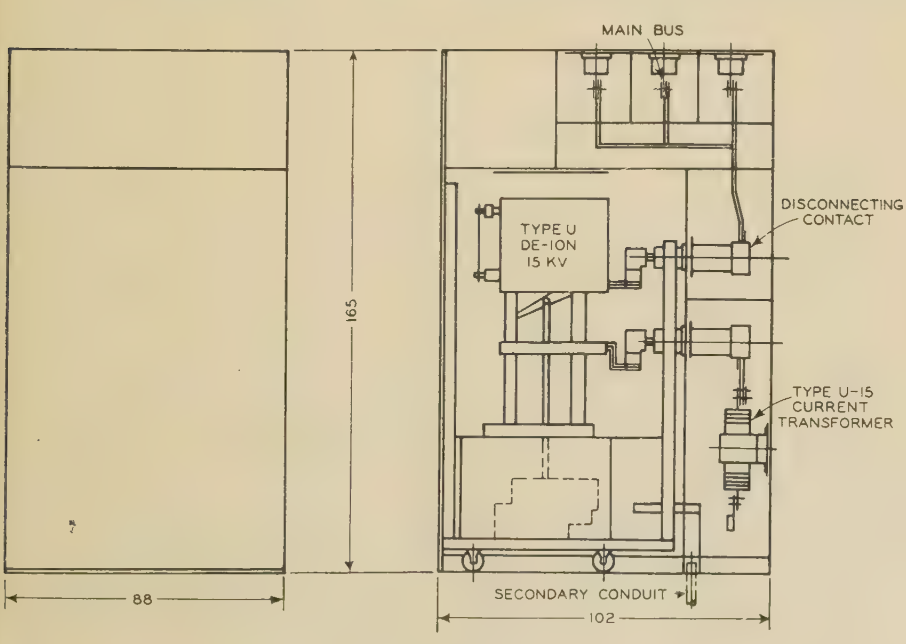

Figure 10. Horizontal drawout metal-clad unit for the De-ion air circuit breaker including well-known features in use with conventional equipment

Discussion

D. C. Prince (General Electric Company, Philadelphia, Pa.): I am sure that the electrical-engineering profession has watched the development of the De-ion breaker with considerable interest since it was first announced at the winter convention of the AIEE in 1929. Since that time added importance has been attached to oilless-circuit-breaker development by a succession of station fires which have not however been attributed to faulty circuit-breaker operation.

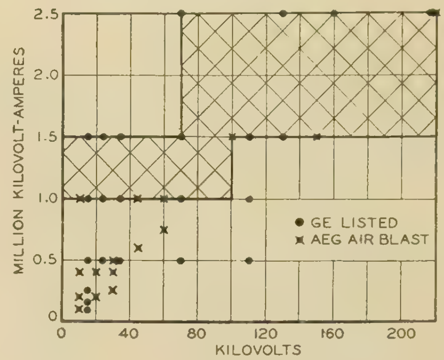

Figure 1. Circuit-breaker ratings

During the same period European manufacturers have been carrying along parallel development of oilless types. There is little doubt that the oil circuit breaker will be superceded by an oilless type if and when such a breaker is developed which offers the service, simplicity, performance, size, and cost of an oil breaker and has in addition no oil. Such a circuit breaker to have an important bearing on switchgear development should satisfy the foregoing criteria over as much of the field as possible.

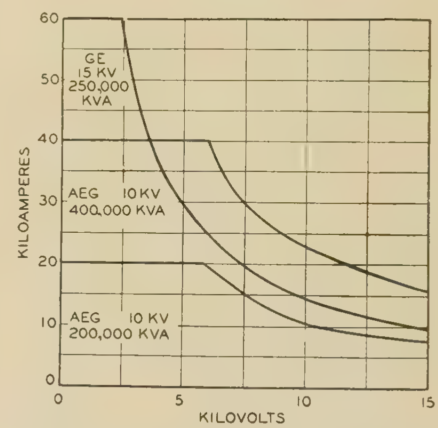

Figure 2. Comparison of interrupting rating in amperes

Figure 1 of this discussion shows the maximum listed rating of standard oil circuit breakers in the United States compared with corresponding ratings in Europe. One is at once struck by the fact that the maximum ratings of European breakers at all voltages are far below those listed and widely used in the United States.

Figure 2 shows the variation in interrupting current at reduced voltage as listed for a standard type of oil circuit breaker compared with corresponding data for European designs. It is again noted that the ability of the foreign designs to handle high currents is far below that of the standard domestic oil circuit breaker.

In the ten years since Mr. Dickinson first described this circuit breaker before this body, maximum interrupting capacity in current seems to have risen from 28,000 amperes to 41,500 amperes and in voltage from 15,000 to 24,000. The oil circuit breaker therefore still occupies the field as the best available means of interrupting all currents and all voltages.

M. H Hobbs (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): It seems to me that one of the outstanding features of the breaker described by Mr. Dickinson is its uniform performance on all currents. This makes the breaker very flexible and permits its application to a wide variety of circuits. One additional application not mentioned in Mr. Dickinson's list is that to circuits supplying arc furnaces, where oil breakers had not proved satisfactory and the operations numbered 75 or more per day. As usual the De-ion breaker is giving a very good account of itself.

Briefly mentioned in the paper were metal-enclosed assemblies incorporating De-ion breakers. The unit is particularly well adapted to cubicles or metal-clad switchgear of the horizontal drawout type. As a matter of fact, if it were not, its value would be much reduced, for modern American practice almost universally requires metal enclosures.

For steel-mill service, requiring more or less frequent disconnecting for safety reasons, and to facilitate the relatively little maintenance required during the regular but short shutdown periods, horizontal drawout metal-clad mounting for the breaker is particularly well suited. Complete separation of the bus and connections from the breaker unit may be provided and phase isolation which this important service demands may also be accomplished. Routine inspection of the contacts after the breaker has been rolled out of the housing becomes a convenient and safe procedure. The same adequate clearances and dielectric strength of insulation which characterize the breaker, are also included in the metal-clad assembly.

Cubicle mounting is more common for powerhouse work involving the heavier breakers and with service which is not so severe as far as repetitive duty is concerned. Isolation of the individual parts of the circuit is no less important, however, and here again the breaker is well adapted to the requirements. To facilitate removal of the arcing chambers, a removable rail is provided, thus giving convenient access to the contacts.

It seems clear that the near future will see the application of this new development to a number of heavier industrial and power-station switchgear installations.

P. H. Adams (Public Service Electric and Gas Company, Newark, N. J.): Mr. Dickinson has presented in his paper a description of the latest development in the air De-ion circuit breaker designed for 15-kv service where interrupting duty does not exceed 800,000 kva. It is to be expected that with further experience and development, the range of this type of circuit breaker will be considerably extended.

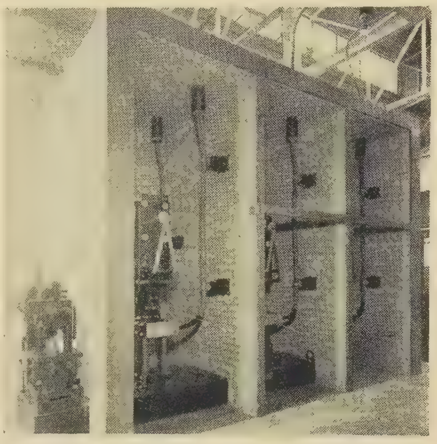

Figure 3

In planning the rebuilding of the high-voltage bus at City Dock, one of our important substations, which supplies a low-voltage network, series street lighting, radially fed power, and rectifiers for street-transport service, a great deal of consideration was given to service protection and fire prevention.

Although the circuit breakers already in use in this substation for the high-voltage bus were of a type that contained only 12 or 13 gallons of oil each, they were obsolete and of insufficient rupturing capacity. The interrupting duty required of the circuit breakers in this substation had increased along with the increase in system generating capacity to a point beyond that for which the existing breakers could be rebuilt.

Since new breakers must be provided, it was decided to use oilless circuit breakers. Accordingly, in 1938, eight 1,200-ampere 15-kv De-ion air circuit breakers insulated for 23 kv and to have an interrupting capacity of not less than 750,000 kva, were ordered for use on the 13.2-kv bus.

A heavy steel base of reinforced channel iron construction was provided for each of these circuit breakers so that the operating mechanism and contact elements could be assembled on it and shipped from the factory ready to place on foundations and have the brick cell work built around it. Removable cover plates on the base give access to the operating shaft. A portable rail and trolley hoist can be attached to the top of any compartment to remove the grid element for inspection. The grid element weighs about 1,000 pounds.

Figure 3 of this discussion shows the circuit breaker in the cells with electrical connections ready for service. The cell and base are arranged for a change of the pole elements to 34.5-kv units in the future.

The series of tests given in table II in Mr. Dickinson's paper were witnessed by us in connection with the development of the pole units for the breakers on this order. From the showing made on these tests, it would appear that progress is being made in the development of the oilless type of circuit breaker. It is to be hoped that further developments will bring about the elimination of oil in circuit-opening devices for indoor use in the not-too-distant future.

There is also need for elimination of inflammable fluid in outdoor equipment. The air-blast circuit breaker gives promise in this field and its development for high-voltage service should be continued.

Multiple-Grid Breakers for High-Voltage Service

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Synopsis: There are occasional demands for high-voltage circuit breakers to interrupt in five cycles or less, and to meet this demand, a "De-ion grid" interrupter has been developed which may be used in multiple unit groups for apparatus rated 138 to 230 kv. Records of field tests up to 2,250,000 kva at 220,000 volts are given, showing interrupting times averaging 2.6 cycles (50-cycle frequency). Cathode-ray oscillograms indicate a comparatively small amount of transient overvoltage, less than would be expected from the usual magnetic oscillograph records. With the fast breaker operation, the apparent system voltage dip was small, and freedom from system disturbance and loss of synchronous equipment is noteworthy.

FOR some years the standard breaker interrupting time for high-voltage circuits has been 8 cycles on a 60-cycle basis or 0.133 second.[1] Individual cases have arisen where faster operation has been required, as for instance, the 3-cycle apparatus on the 287-kv Boulder Dam lines.[2] Three-cycle performance at very high voltage introduced special problems and increases the cost of the apparatus involved to a marked degree. On the other hand, it is now feasible through improved interruption methods, to considerably better the 8-cycle standard at a relatively small increase in cost. For the future, therefore, 5-cycle performance on a 60-cycle basis or 0.083 second breaker time from trip coil energization to arc interruption may be procured readily in voltage classes 138 to 230 kv. A multiple-unit De-ion grid assembly to meet these requirements has been developed and tested in the laboratory and the field.

The new interrupter assembly is made of units, each built from a design which has a previous record of satisfactory service at lower voltages. These units are arranged to operate simultaneously to give, at relatively high speed, a series of low-voltage arcs. This arrangement has several advantages: (*a*) each small unit can be tested to the limit of the laboratory power available, (*b*) damage to an individual unit does not impair the operation of the others, and (*c*) mechanical details are readily accessible for inspection and repair. The *CDH* grid after extensive high-power laboratory tests, both in this country and in Europe, was standardized upon two years ago for 46- and 69-kv service. At that time, investigation of voltage-interrupting ability for short grids of this type showed possibilities of its use in the high-voltage field. A small grid designed for 34.5-kv service proved actually able to handle 66-kv circuits and automatically suggested the use of this small unit in multiple assemblies for the high-voltage classes.[3]

Figure 1. Typical three-pole 230-kv oil circuit breaker

Some changes in assembly naturally arose, since at low voltage, the *CDH* grid has been handling heavy currents, and at 220 kv the current-interrupting requirement is in the order of only 6,000 amperes. The finger contact previously used is no longer necessary for conductivity and a simple butt contact not only is adequate in that sense, but provides quicker parting time once the breaker is unlatched. The butt contact also is more economical in space which is particularly desirable when several units are built into a multiple assembly.

The venting of gases from the arc interruption also required consideration. The two vents near the top of the stack which had been found desirable with currents up to 40,000 amperes were retained. The lower part of the stack was entirely closed, except for a rectangular opening in the bottom plate into which the moving contact fits rather closely. This results in sufficient gas pressure within the stack at the lighter currents involved with 220-kv operation, without exceeding a pressure which can be handled safely by the enclosing structure. Each stack is built up of hard fiber plates, tied together with four wood-base Micarta studs. Iron plates are used for directing the arc as in the predecessors of this line of De-ion grid stacks. In general, the assembled unit has much of the appearance of a 34.5-kv stack except that metal top and bottom end plates are employed.

A group of three of these grid units per stationary contact (six per phase) comprises the interrupting element for 220-kv service as shown in figure 2. A similar group of two (four per phase) can be used for 138-kv apparatus, as test results discussed later will indicate. The mechanical linkage at the top of the tank is quite similar to the conventional breaker, except that special high-strength steel is used in places where a saving in mass makes its use justified. Light weight and speed of opening were prime considerations in the design. The usual form of lifting rod and cross bar (figure 3) is used, but the cross bar does not make and break the circuit as in the usual two-break switch. It is used to pick up (in closing) an insulating operating rod to which are attached, through a lever ratio, the three moving contacts of each assembly (figure 3). In opening, the three contacts are moved rapidly under the effect of the butt contact springs and also a substantial spring at the top of the operating rod, the lever ratio making it unnecessary for the lift rod and cross bar to be accelerated at such a high rate.

Considerable effort was put into the design of these fast-moving contact parts as the entire short-circuit interruption takes place within the travel of these small moving contacts and speed consequently is most important. Heat-treated aluminum contacts are used to provide the necessary conductivity and at the same time to secure the greatest speed

Paper number 39-36, recommended by the AIEE committee on protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 25, 1938; made available for preprinting December 20, 1938.

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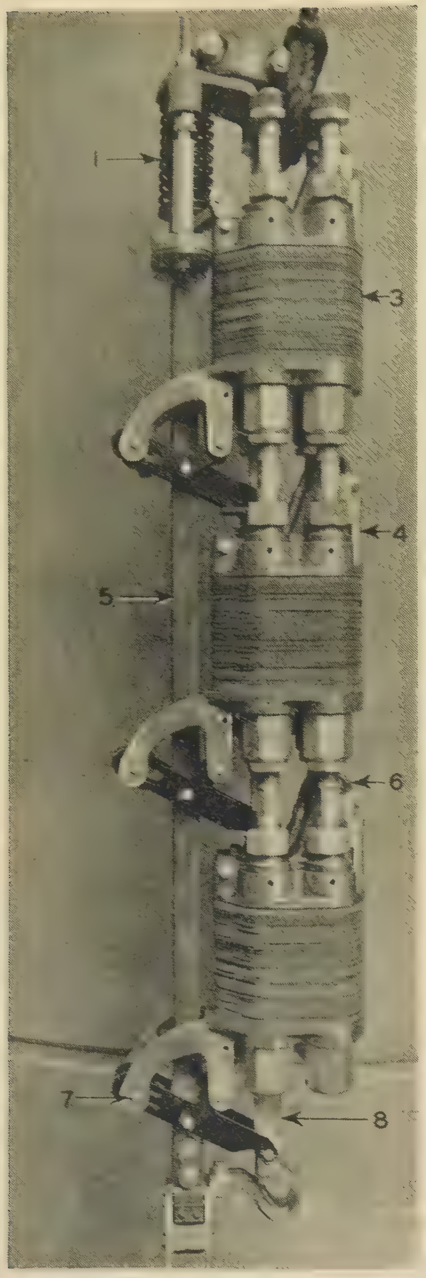
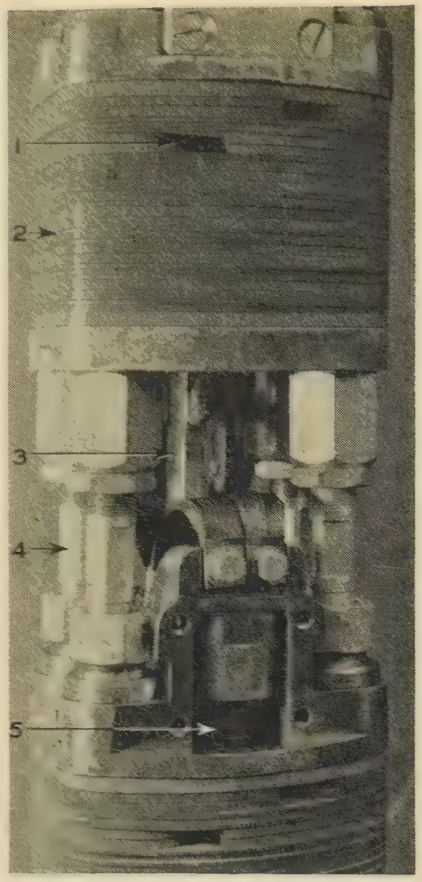
1. For all numbered references, see list at end of paper.



(a)

1—Accelerating spring
2—Dash pot
3—Grid unit
4—Cover plate over stationary contact
5—Operating rod
6—Adjustment
7—Eccentric pin
8—Moving contact



(b)

1—Vents
2—"*CDH*" grid
3—Moving contact
4—Adjusting jacks
5—Stationary contact spring

Figure 2. Stationary contact assembly for six-grid 230-kv breaker

with least effort, through a reduction in mass. Long life of contacts is assured through the use of tungsten alloy tips to take the burning. The accelerating force of the butt contact springs acts through the first fraction of an inch of contact travel and serves to get the parts into motion as well as providing low-resistance point of contact. Another spring at the upper end of each operating rod acts through the entire stroke and insures that sufficient speed of motion will be maintained until the arc is extinguished. An oil-dashpot effect is incorporated into the upper part of the assembly, bringing the contacts to rest without excessive slam and without rebound. After leaving the interrupting elements of the stationary contact, the cross bar continues to move through a considerable body of clear oil, introducing an oil disconnect into the circuit in addition to the active break of the interrupters.

Alignment of the three grid units of each assembly is secured by adjustable tie rod connectors, providing for correct spacing of the units as well. Fine adjustment of each contact position is easily obtained by turning the eccentric hinge pin (figure 2).

A stationary contact is enclosed within the upper cast end plate associated with each stack. It is hinged at one end, backed up by a substantial contact spring, and protected against excessive burning by means of a face of tungsten alloy, silver soldered in place. For convenience in inspection and maintenance, this contact can easily be removed and replaced together with its spring and flexible shunt, without removing other material, or disturbing adjustments.

Preliminary testing in the high-power laboratory covered a wide range of short-circuit currents and voltages. One series of tests, closing and opening, made with 132 kv impressed across a single pole of a 220-kv breaker, showed uniform duration of arcing from 500 amperes up to 3,000 amperes. Other runs at 88, 66, and 44 kv up to 8,000 amperes gave similar performance, that is, uniform in time up to the highest current interrupted. These results are shown in curves 1–2 of figure 7.

The effect of high kilovolt-amperes per unit was also demonstrated by a series of tests in the laboratory using only two of the six grids. Here again, a very uniform curve of interrupting time against current was obtained up to currents well over 7,000 amperes at 66 kv and 44 kv (see curve 3 of figure 7).

In all these tests, the arc was extinguished within half the stroke of the moving contacts. The oil depreciation was so slight as to be not measurable.

Another series of tests shown in figure 8 was made at approximately 1,000 amperes, at voltages varying from 110,000 to 230,000 volts. All results, when plotted between applied voltage and interrupting time, fell within an envelope not more than 0.02 second wide. This shows the uniformity of operation over a wide voltage range. A similar series also shown in figure 8 with only four of the six grids operating was carried up to 176 kv before

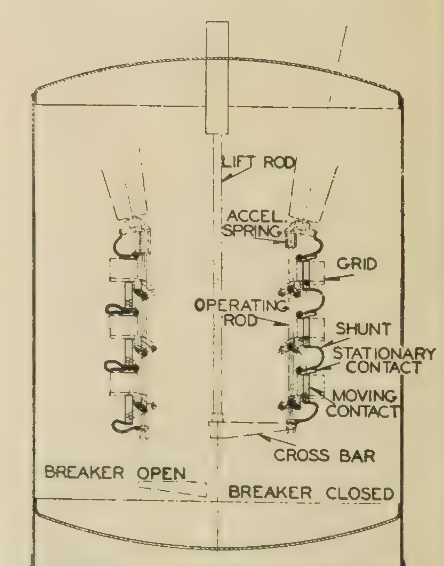


Figure 3. Schematic arrangement of contacts—220-kv, six-grid, in open and closed positions

the breaker failed to interrupt within the grids. The very satisfactory performance on these tests up to well over 132 kv, together with the testing of individual grids to 40,000 amperes at lower voltage, justifies the application of a four-grid assembly to 132-kv high-speed work.

As a result of this laboratory demonstration, arrangements were made for further tests on a three-pole breaker at the Laguna Bell substation of the Southern California Edison Company. This station is centrally located in the system and can produce short circuits of approximately 2,250,000 kva, at an operating voltage of 220,000 volts. A set of contacts, together with the necessary mechanical parts, was sent out from the factory and installed in a type *G-22-A* 187-kv three-pole breaker, which had been in service approximately ten years. Single-phase-to-ground faults were applied, sometimes by the test breaker, and sometimes by a separate closing breaker, to the third pole of the breaker further away from the mechanism. A protective relay operating from overcurrent in the ground lead, tripped the breaker in 0.005 to 0.010 second. A six-element magnetic oscillograph and a cathode-ray oscillograph were used simultaneously to record fault current, voltage across the breaker, relay time, trip coil current, and position of lift rod in one pole of the breaker. Changes in magnitude of fault current were secured by switching lines and transformers at various parts of the system.

Cathode-ray oscillograms recorded bus voltage to ground on the faulted phase, during the period of arcing, and for a few cycles thereafter. Each record shows a central zero line indicating a closed contact; another trace a small distance away indicating arc voltage, so small as to be scarcely noticeable; and the open-circuit recovery voltage which is sine wave after a short transient generally not exceeding one-half cycle. The maximum peak of recovery voltage obtained on the whole series of tests occurred on the lightest short-circuit values of approximately 200,000 to 225,000 kva. On one of these light short circuits an overvoltage of 170 per cent above normal crest voltage was obtained. For all heavier short circuits, however, the recovery voltage did not exceed 10 or 15 per cent above normal crest. On the lightest short circuits also the rate of rise of recovery voltage was greatest, being approximately 2,300 volts per microsecond. On the heavier short circuits this rate was reduced being approximately 500 volts per microsecond. It is interesting to note that these actual field-test cathode-ray data substantiate calculations made for similar conditions and

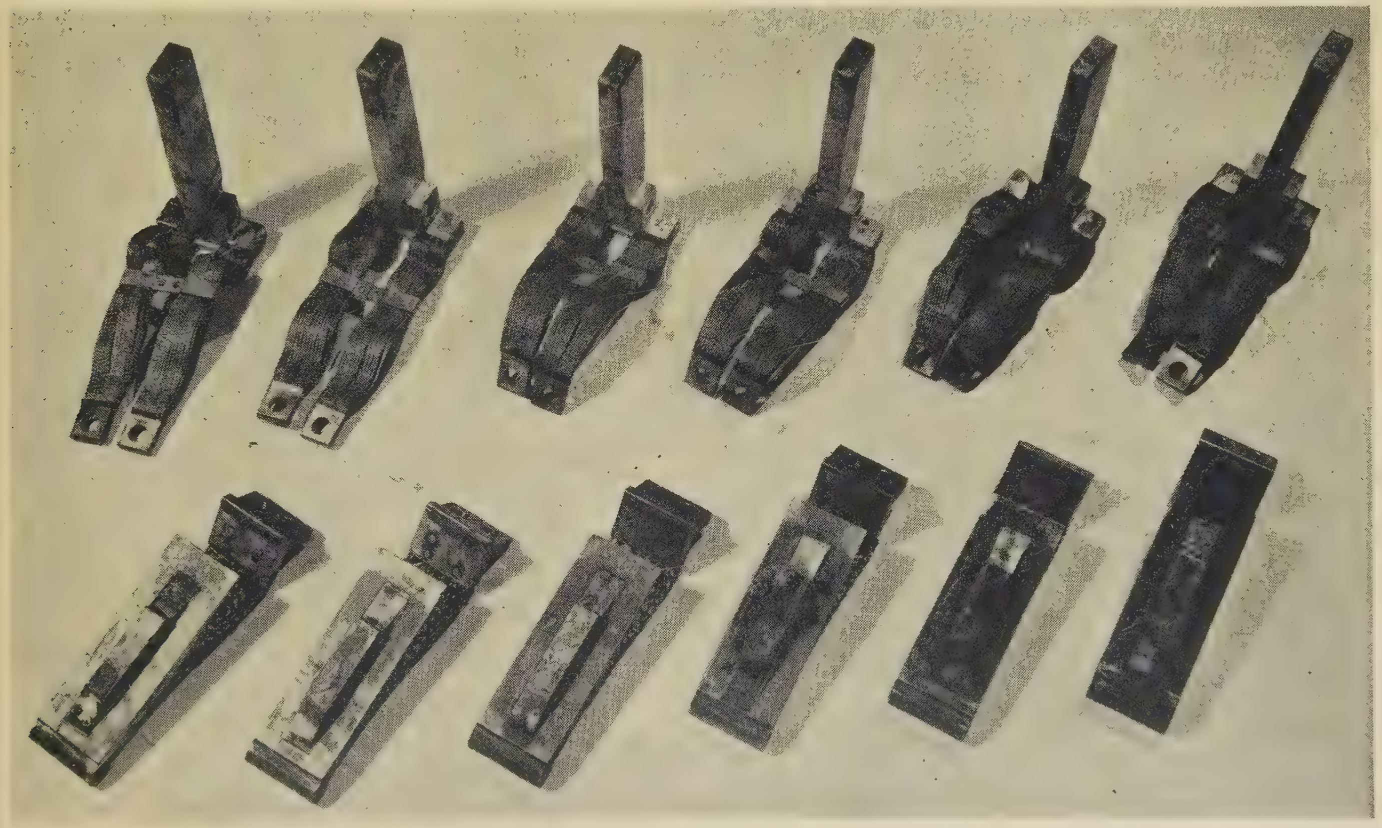
Table I. Southern California Edison Company 220-Kv 50-Cycle Tests

Westinghouse Type *G-22A* 187-Kv Oil Circuit Breaker; October 16, 1938

Test	Shot*	Current Interrupted (Amperes)	Equivalent Three-Phase Kilovolt-Amperes	Duration of Short Circuit (Cycles)	Breaker Interrupting Time (Cycles)
1 *W*	O	620	225,000	3.7	3.4
1 *AW*	CO	565	205,000	3.9	3.6
2 *W*	O	1,600	590,000	3.1	2.7
2 *AW*	CO	1,520	553,000	3.0	2.6
3 *W*	O	2,780	1,011,000	3.4	3.0
3 *AW*	CO	2,960	1,135,000	1.5	1.1
4 *W*	O	5,220	1,900,000	3.1	2.6
4 *AW*	CO	5,330	1,938,000	2.5	2.2
5 *W*	O	6,000	2,250,000	3.1	2.6
5 *AW*	CO	6,000	2,250,000	2.6	2.1
GW	Opening 22.9 miles of line— 15 amperes				3.1
MW	Opening 118.1 miles of line— 77 amperes				4.8
BCW	Opening 246.4 miles of line—150 amperes				5.6

* "O" indicates "opening"; "CO" indicates "closing-opening."

Figure 4. Contacts after field test—ten shots 565 to 6,000 amperes



already presented before the AIEE.[4] Cathode-ray oscillograms of field conditions should be particularly useful in interpreting magnetic oscillograms and Hall recorder films—a more general use of cathode-ray records by operating companies is recommended.

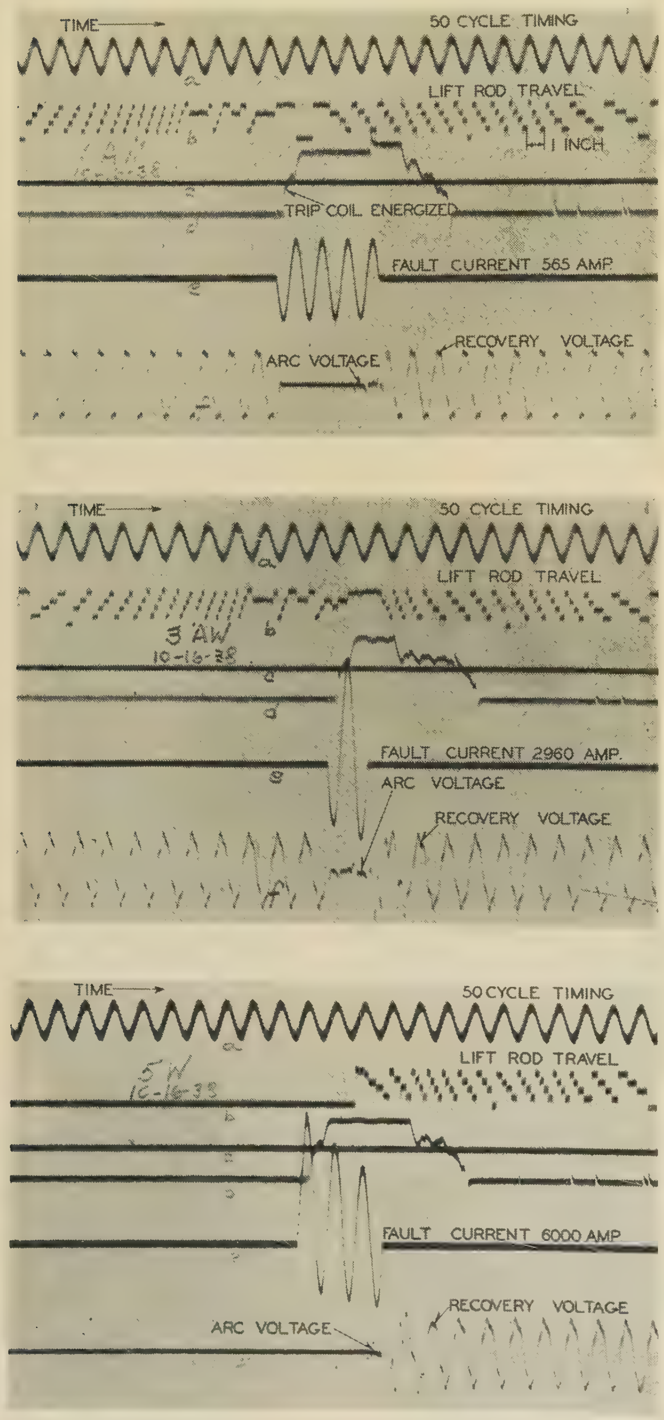


Figure 5. Representative oscillograms (220 kv, 50 cycles)

a—Timing wave
b—Lift rod travel
c—Not used
d—Relay and trip coil current
e—Fault current
f—Line-to-ground voltage

1 AW—A CO shot interrupting 565 amperes

3 AW—A CO shot interrupting 2,960 amperes

5 W—An O shot interrupting 6,000 amperes

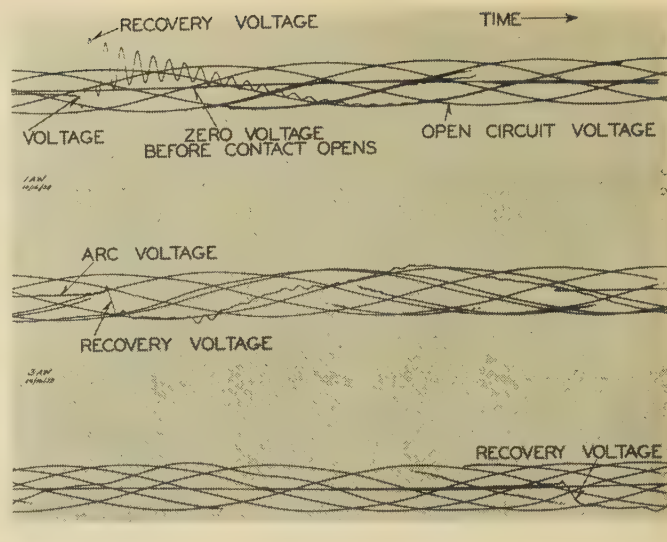


Figure 6. Cathode-ray oscillograms—for tests illustrated in figure 5

Figure 6 shows three typical cathode-ray oscillograms, with the several parts of each record identified so as to point out the transition from zero voltage before the contacts open, to arc voltage, to the transient at the instant recovery voltage appears and finally, the sine-wave open-circuit voltage. Since the film was rotated on a drum, each trace as it leaves the right-hand end of the oscillogram can be picked up again at the opposite end to form a continuous record.

The results of this series of field tests are shown in the tabulations (table I) and typical magnetic oscillograms are also shown (figure 5). Each oscillogram gives a time record of the position of the breaker contacts (trace *b*) before and after the fault current (trace *e*). The rise of current in the trip coil circuit is shown (trace *d*), lagging the start of fault current by approximately one-third cycle. Movement of the breaker mechanism follows directly thereafter, and arc voltage and recovery voltage (trace *f*) appear as the fault current is extinguished. Voltage records on these magnetic-oscillograph records are somewhat distorted, due to inaccuracies in the measuring circuits, and a more accurate record is obtained from the cathode-ray oscillograph. Values of current and equivalent kilovolt-amperes are given in table I, showing interrupting time averaging 2.6 cycles (50-cycle frequency, or 3.1 on a 60-cycle basis) and ranging from 1.1 cycles at 1,000,000 kva to 3.6 cycles at 200,000 kva.

Importance has been attached recently to the more prompt interruption of charging currents on high-voltage lines than has been practical with old forms of interrupter. As part of the field tests described hereinbefore, a group of charging-current interruptions were made, the results of which are shown in table I, in-

dicating breaker time from energization of trip coil to arc extinction ranging from 3.3 to 5.6 cycles on a 50-cycle basis. The 246-mile stretch of 220-kv line used in these tests is, we understand, the longest 220-kv line on which charging-current interruption has been made under test conditions, the charging current being 150 amperes. Cathode-ray oscillograms of these charging tests show transient voltage phenomena during interruption up to 100 per cent above normal line-to-ground values.

Inspection of contacts showed no excessive burning of metallic parts or fiber plates, nor was there any noticeable oil depreciation. The dip in voltage on the system was so slight as to cause no trouble to connected synchronous apparatus. It is evident that the requirements of five-cycle operation for high-voltage equipment have been met with considerable margin.

Short-circuiting the heart of a 220-kv operating system under load conditions up to three-phase equivalents of 2,250,000 kva involves careful planning and some risk-taking even on a system as robust and well operated as that of the Southern California Edison Company. The difficulties are probably better appreciated by those skilled in system operation under fault conditions than they are by the apparatus designers. However, both have a keen interest in such field tests as they afford the only way of verifying the results obtained in high-power laboratories. We express only the general sentiment of the industry in acknowledging the contribution made to the switchgear art by the Southern California Edison Company in subjecting their system to the tests referred to in this paper.

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4. Power System Transients Caused by Switching and Faults, R. D. Evans, A. C. Monteith, and R. L. Witzke. AIEE Transactions, volume 58, 1939, pages 386–97 (August section).

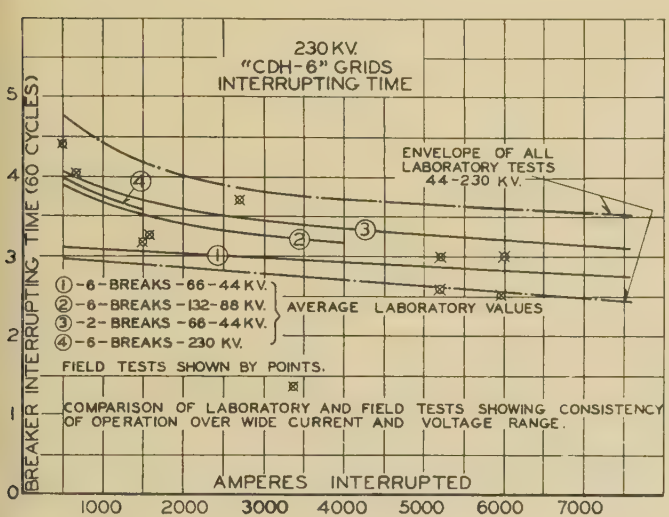

Figure 7. Interrupting performance with varying current

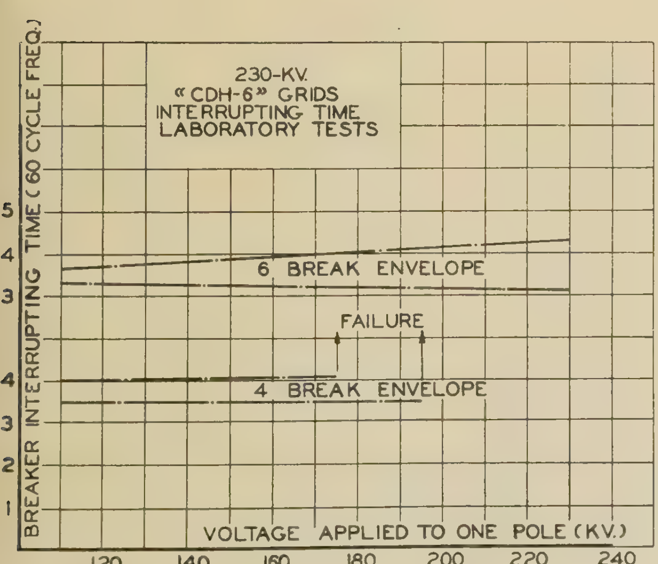

Figure 8. Interrupting performance with varying voltage

Discussion

H. K. Sels (Public Service Electric and Gas Company, Newark, N. J.): Messrs. MacNeill and Hill have presented a very ingenious arrangement of De-ion grid contacts which will speed up and improve the performance of high-voltage breakers. I should like to present some remarks from the point of view of system stability, relay settings, line damage, and breaker rupturing ability using such an arrangement. The arrangement shown decreases the duration of short circuit from around six to eight cycles for the present standard breaker to something less than three cycles. All of these speeds are a great improvement over older types of breakers which have interrupting speeds around 20 to 30 cycles. Naturally there is a great reduction in damage to line conductors and insulators by decreasing the time of interruption to three or eight cycles. However, to obtain fully these benefits with high-speed breakers, it is desirable to use impedance step-type, carrier-current, or pilot-wire high-speed relays to keep the total interruption time as low as possible.

From a system stability standpoint when a system is short-circuited the reactive kilovolt-ampere demand on the system increases enormously and the kilowatt demand on the system may increase slightly or decrease. In any event the distribution of these loads among the generators on the system differs considerably from the load conditions which existed prior to the application of the short circuit to the system. As a consequence certain machines become unloaded and speed up while others tend to slow down. If a short circuit is not removed rapidly enough from the system the phase angles between the advancing and retarded machines will become so great that they will pull out of step. Studies show that great improvement is obtained in removing faults within one-fourth to one-half second but improvement below this point is questionable economically. Unquestionably if we could remove a fault from the system instantaneously we would not have a stability problem but it is doubtful if any such ideal could be approached from an economic standpoint. As a matter of fact we doubt whether there is any advantage of improving speeds below eight cycles.

The rapid removal of a fault from the system and reclosure of the line has been proposed as a means of improving service on our transmission systems. Sufficient experience has not been obtained with this arrangement to warrant such a policy. At this time I doubt very much whether it can be proved economical. In general it seems to me that this field is the only application open to a three-cycle breaker. There are so many improvements available which can be applied to transmission lines generally, such as ground wires, lowering tower-footing resistances, arcing devices, and high-speed relay systems, decreasing the inter-

rupting speed to three cycles is more or less of a refinement for limited application.

Another factor to be considered in extremely high-speed fault interruption is the fact that as soon as you interrupt a fault at less than approximately six cycles the fault current which must be interrupted is asymmetrical and may be considerably above the symmetrical value. This is illustrated in the test oscillograms, figures 5*b* and 5*c*. In other words, within certain limits the faster the fault is removed the greater rupturing capacity it is necessary to provide in the breakers so that while it is advantageous to decrease the time of fault interruption in one respect it is disadvantageous in another. I do not wish to discourage the development of high-speed arc-interrupting devices but do wish to point out those factors which must be considered in their development. The opinions which I have expressed here apply not only to the developments which Messrs. MacNeill and Hill have described but to developments of other designers as well. I believe that if these developments give us a breaker of better interrupting ability it is of a great deal more advantage than the decreased time of arc interruptions obtained. Our sad experiences in the past of the manufacturers derating of breakers, many of which are too recent, emphasize the necessity of improvement along these lines rather than the necessity of further improvement in speed of fault interruption. Of course, if the improved speed in the breaker is inherent with its interrupting ability, that is another story and a welcome one because then we can control the time of arc interruption in relay settings.

D. C. Prince (General Electric Company, Philadelphia, Pa.): The authors are to be congratulated on the high degree of perfection achieved in the development of the De-ion grid. Examination of the successive papers describing the De-ion grid, which have appeared since this device first made its appearance in 1930, shows a steady evolution from the open slots of the earliest designs toward more and more complete enclosure of the arc in an explosion chamber. In the present paper this enclosure seems to be complete and one wonders whether the iron plates in the assembly need be longer retained. If it could be shown that operation is unimpaired by omitting the iron, which is very probable at these low currents, the question might be asked whether there is any distinction between the device of the paper and an explosion chamber.

H. A. Lott (Southern California Edison Company Ltd., Los Angeles): In October 1936, the first of a series of field tests at 220 kv were made on an oil circuit breaker at the Saugus substation of the Southern California Edison Company Ltd. During the ensuing year, 39 single-conductor-to-ground short circuits, varying in value between 0.3 and 1.7 million equivalent three-phase kilovolt-amperes were imposed on the 220-kv system at Saugus and cleared without inconvenience to consumers' service. The number of short circuits at each of the four duties which were used to test three different types of 220-kv breakers are shown in table I of this discussion.

An analysis of the Saugus tests led to the following conclusions:

1. If the over-all time of clearance is in the order of five to eight cycles, similar tests can be carried to the maximum values of 2,250,000 kva without inconvenience to consumers' service.

2. The final proof of a circuit breaker's performance occurs when the breaker is required to interrupt a short circuit at or near its rated capacity. The most practical way to apply such proof is through the medium of staged field tests on a high-capacity transmission system. While both the design and laboratory tests may be carried out with painstaking attention to every detail, such tests, because of limitations in the capacity of the laboratory equipment, fail to disclose occasional weaknesses which are only disclosed when a breaker is tested at or near its rated capacity. Usually such weaknesses are corrected by relatively minor changes in design.

In our discussion on "A New Multibreak Interrupter for Fast-Clearing Oil Circuit Breakers," AIEE Transactions, December 1938, the procedure for conducting a short-circuit test at Saugus was outlined, together with some comments on the need for high-speed switching and the experience with high-speed switching on the Southern California Edison transmission system.

The operating results of the Saugus tests were so satisfactory and the interest was so keen that there was no hesitation in staging the next series of tests at Laguna Bell substation. Laguna Bell is at the load center of the 220-kv system, and the 2,250,000-kva duty is the greatest concentration of power at 220-kv available at any location.

The procedure used to conduct a short-circuit test at Laguna Bell was practically identical with the procedure used at Saugus. At Laguna Bell five different line and bus switching combinations were used to obtain five values of short-circuit current. However, since each test presents certain minor variations, a complete analysis was made, and a printed program outlining each step in the operating procedure was distributed in advance of each test to all station operators and engineers concerned.

Table I. Short-Circuit Tests at Saugus Substation

Number of Short Circuits	Equivalent Three-Phase Kilovolt-Amperes Interrupted (Millions)
8	0.3
6	0.5
6	1.0
19	1.7
39	

During 1938 field tests were made on four different types of 220-kv breakers at Laguna Bell up to the maximum available duty of 2,250,000 kva. The number of short circuits cleared for each of the five values of fault current are given in table II of this discussion.

In one test, three interrupters, each slightly different from the others in design, were installed, one design in each of the three separate tanks of the breaker, and were tested the same day. All three tanks were tested at the same duty and the results were compared before imposing the next higher value of short-circuit current. One design proved superior, and was the only one tested at the maximum value of 2,500,000 kva.

The system performance while the maximum duties were being cleared at Laguna Bell confirmed in every detail the conclusions derived from the Saugus tests. There were no dips in the system speed and no consumer complaints when the short circuits were cleared in eight cycles or less. In a few cases the over-all clearance time exceeded eight cycles. When the clearance time was more than eight cycles, the short circuits of both the 1,750,000- and 2,250,000-kva values caused sufficient voltage dip to drop some motors which were equipped with instantaneous undervoltage release coils. There were no indications of instability following any one of the 77 test short circuits.

Table II. Short Circuit Tests at Laguna Bell Substation

Number of Short Circuits	Equivalent Three-Phase Kilovolt-Amperes Interrupted (Millions)
22	1/4
16	1/2
18	1
13	1 3/4
8	2 1/4
77	

The test on the multiple-grid breaker for high-voltage service was made at Laguna Bell on October 16, 1938. The design features and results are presented in the paper under discussion. The tests progressed rapidly and as outlined in a program written in advance.

The results from the standpoint of system operation were excellent. There was no dip in the system speed, and the maximum voltage dip of five per cent was recorded when the 2,250,000-kva fault was being cleared in an over-all time of 3.1 cycles. There were no consumer complaints and no inconveniences to service since the short circuits were cleared too rapidly to permit the operation of instantaneous undervoltage release coils. Most consumers were not aware that tests were in progress as the flicker of lights which indicated when the short circuit was imposed and cleared was barely perceptible.

The authors are to be congratulated for their presentation of an interesting paper, and we are pleased to note the progress that has been made by all switch manufacturers in the development of high-speed, high-capacity circuit breakers designed to add further improvements to the continuity and quality of electrical service.

F. W. Gay (Public Service Electric and Gas Company, Newark, N. J.): Where breakers of this type are needed, they are needed very badly and the breaker under discussion seems to be a happy solution to a difficult problem.

It is suggested that one of the most necessary applications for this breaker is on circuits having high capacity, as for instance 132-kv cables. If a breaker of slow rupturing time is used on cables, restriking will

occur. It is believed that multiple breaks, combined with high speed, will greatly reduce restriking.

The advantages of a super-high-speed breaker for ordinary duty have been greatly exaggerated. It is believed that there are a comparatively small number of applications where there is any great advantage from a stability standpoint in clearing a faulted circuit in much less than 20 cycles. If the total clearing time is much under 15 cycles, the rupturing occurs on the subtransient portion of the decrement curve and the current that must be ruptured increases rapidly when times of clearing are much less than ten cycles. If the current to be ruptured is increased, say ten per cent, by shortening the time of clearing the circuit, then the rupturing capacity of the breaker must be increased approximately ten per cent. If no credit is to be given the breaker because of less destruction at the point of fault, due to this shorter time of clearing, and if the high speed is not required for stability, then a fast-clearing breaker of given rupturing capacity must sell for less money than the standard eight-cycle breaker.

Every company should have swing curves for faults on various parts of the system. It is believed that when these swing curves are examined it will be found that there are very few locations, barring generating stations far from their load, where loss of stability or scrambling will occur in much less than three-quarters of a second for a double-line-to-ground fault. Certainly in this case a 20-cycle clearing time is short enough from a stability standpoint.

Better and faster relaying offers a more promising field for development.

W. A. Lewis (Cornell University, Ithaca, N. Y.): The discussion this afternoon has been primarily a description of the mechanical details of the new breaker design, and the performance of these circuit breakers under test conditions. It is interesting to consider the benefits offered to utility system operating engineers by the improvements in the circuit breakers. A large number of tests have been made by the Southern California Edison Company on circuit breakers of various designs. In some of the previous tests, existing breakers having a considerably longer operating time were used in the test. It was found that when the total time of fault clearing exceeded 12 or 14 cycles, a short circuit could be felt by customers on the system, and some motors having instantaneous undervoltage release devices would be disconnected. On the other hand, in tests involving faster operation no trouble at all was experienced by any of the customers on the system. In considering these tests, it should be remembered that all of the faults were single line to ground, and it can readily be seen that for the more severe faults, such as two line to ground, the high speed of operation is essential to avoid customer disturbance when the fault is at a location producing the maximum reduction in voltage. Computations on this and similar systems have, of course, shown that such speeds of fault clearing are necessary in order to maintain system stability. On systems having a large percentage of hydroelectric generation and long transmission distances, rapid fault isolation is much more essential than it is for those systems having all generation by steam, and with shorter transmission distances. However, as system interconnections are increased, rapid fault isolation will become more important even on those systems generating entirely by steam.

Another feature of the paper which is of considerable interest is the fact that the recovery voltage rates indicated by the cathode-ray oscillograms agree fairly well with the calculated recovery rates. A great deal has been done on developing methods of calculation, particularly those using the a-c network calculator, but there are relatively few instances where a direct comparison between calculation and test have been made. It is gratifying to note the satisfactory agreement contained in this case.

The third feature of interest to utility engineers is the fact that these new interrupting devices can be satisfactorily applied to many existing circuit-breaker designs, so that when the improvement in speed of fault clearing becomes necessary, it can be obtained without complete replacement of existing circuit breakers.

W. F. Skeats (General Electric Company, Philadelphia, Pa.): The reference to agreement of the test cathode-ray data with calculations presented in the paper by Messrs. Evans, Monteith, and Witzke is somewhat puzzling in view of the fact that the cathode-ray oscillograms presented in this paper are for short-circuit interruptions, whereas the calculations of Messrs. Evans, Monteith, and Witzke apply to the disconnection of an open line.

It is true in general, as has been pointed out many times in the literature, that an increase in the number of transmission lines connected to a bus tends both to increase the current available and to decrease the recovery voltage rate, so that there does tend to be in many cases a reduction of recovery rate associated with an increase in short-circuit current. Nevertheless, the tests made at the Philo plant of the American Gas and Electric Company as early as 1930 indicate that it is still possible to get high recovery rates with high values of short-circuit current. It would be interesting to know how the device described by Messrs. MacNeill and Hill would perform under such circumstances.

R. C. Van Sickle (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): As an associate of Messrs. MacNeill and Hill, I have had the opportunity of studying in detail the cathode-ray oscillograms of the charging-current interruptions referred to in their paper. Such oscillograms are of considerable interest in showing the voltage existing during switching transients and I wish to call them to your attention.

Figure 1 of this discussion is one of the oscillograms taken during the disconnection of 246 miles of 220-kv line. This is the highest-capacity test of this type ever made. The original oscillogram is about 18 inches long and was made on a rotating drum by a single element recording the voltage to ground of the line being disconnected. The oscillograph was connected to the line close to the circuit breaker.

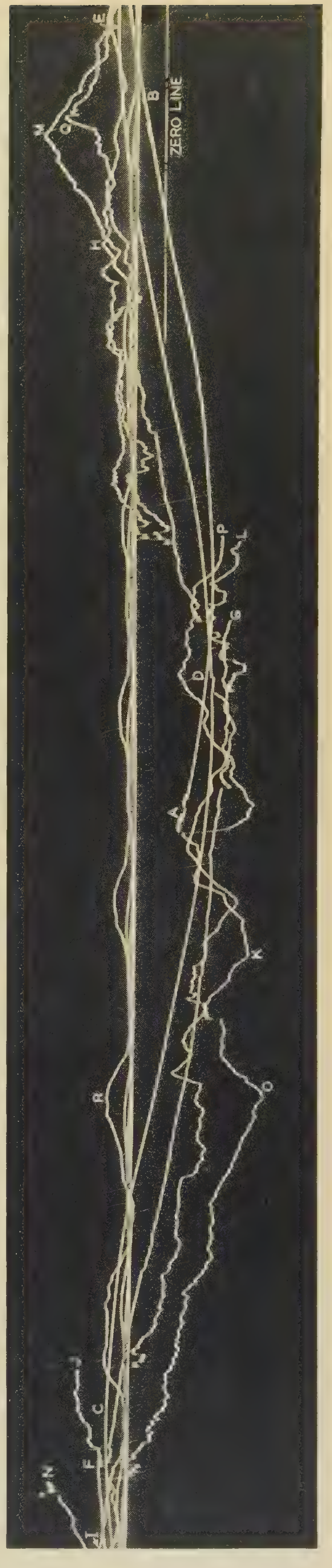

Figure 1

The sine waves show the normal system line-to-ground voltage and the zero line is located midway between crests.

The trace starts at A and moves to the right edge B, reappearing at the corresponding point C at the left edge. The first clearing occurs at D, probably with a very small contact separation since the arc restrikes after a very short interval. The trace goes off the film at E and reappears at the left. After a brief interruption, the arc restrikes at F. Both of these early interruptions are of such short duration that they do not appear as current interruptions on the magnetic oscillograph film and cause no appreciable voltage transient.

At the next crest of voltage the circuit is interrupted for about a tenth of a cycle. The transient following the restrike at G reaches at H a voltage which equals 130 per cent of the normal crest of line-to-ground voltage. The trace reappears at I and a restrike occurs at J. The following crest K reaches a value of 180 per cent. After the restrike L the maximum voltage obtained on the test, 250 per cent, is reached at M. The arc restrikes at N and the voltage reaches 215 per cent at O. The last restrike P produces a voltage of 210 per cent at Q and the transient dies out in an oscillation R leaving the line charged to about 60 per cent of the normal crest value.

The interruption was accomplished with two interruptions of negligible duration and five which were followed by restrikes which caused overvoltages. The overvoltages reached 120, 180, 250, 215, and 210 per cent of the normal crest of line-to-ground value on successive restrikes and caused no trouble on the system.

H. P. St. Clair (American Gas and Electric Service Corporation, New York, N. Y.): I should like to bring out briefly three or four points in connection with the paper by Messrs. MacNeill and Hill describing a new multiple-grid oil circuit breaker for high-voltage service.

While you are all aware that there has been an increasing interest displayed in this country in the development of oil-blast circuit breakers along lines which are a modification of European practice, at the same time the paper you have heard today, coupled with papers describing somewhat parallel developments presented in the last summer convention of the AIEE, gives evidence of the ingenuity of our American engineers in making notable advances in oil circuit breakers along conventional lines. Fortunately, in this country we are free from most of the restrictions—economic, military, or otherwise—which have forced European practice to a large extent away from oil, so that we are able to go ahead and develop the maximum inherent possibilities of oil circuit breakers.

As a result of this, I believe that the situation in this country is a particularly healthy one in that both of these developments, the air-blast and improvements in conventional oil circuit breakers, can go on in parallel without undue haste or pressure on either one. In this way, we are more likely to realize the maximum possibilities of each of these lines of developments.

When the Boulder Dam breakers were designed and built, a drastic step was taken in going from the standard eight-cycle breaker to the three-cycle breaker which that job required. This step was also a very costly one. I believe it is therefore a very sound and desirable procedure to develop a breaker which falls in between these two categories, namely, the five-cycle breaker, which apparently can be done with very little increase in cost over the present eight-cycle breaker. At the same time the gain in speed is a very substantial one and while its importance may not be fully apparent to all users at the present time, nevertheless we believe that it is certain to become more and more important as time goes on. On our own system, this increase in speed is already of considerable importance in connection with the application of ultrarapid reclosing of circuit breakers which has now passed beyond the pioneering stage and is taking its place as an important part of our system planning.

J. B. MacNeill: The field of switchgear may be divided into three principal parts as follows:

Low-voltage industrial

Medium-voltage powerhouse and substation

High-voltage transmission

The three papers presented as a group respectively by Ludwig and Grissinger (pages 414–20), Dickinson (pages 421–6), and MacNeill and Hill outline progress in each of these divisions.

The last-mentioned paper is timely in view of the discussion in engineering circles of the speeds which are necessary in high-power circuit breakers for modern system operation. The diversity of opinion on this subject seems remarkable until the variety of system operating conditions is fully considered. On some systems the present eight-cycle standard is more than adequate because of the solidity with which the system is tied together. There will be an increasing number of cases, however, where severe short circuits involving more than one phase wire will demand faster breaker operation than eight cycles. For such service five cycles seems a reasonable standard, as it can be obtained without marked increase in cost and with reliable equipment permitting numerous operations and providing high-speed reclosing.

A feature of this paper is the data from cathode-ray oscillograph records taken simultaneously with the magnetic oscillograph records under various conditions of circuit opening from charging currents up to a dead short circuit of 2,250,000 kva direct on Laguna Bell bus of the Southern California Edison Company. These records are of particular interest in view of the discussions now before the Institute on possible rates and magnitudes of transient voltages during switching operations. The values of switching transient voltages given in this paper are reassuring to American operating people using grounded neutrals. We are assured that these values are consistent with the results given by Evans, Monteith, and Witzke and Van Sickle. An increased use of the cathode-ray oscillograph under fault conditions on operating systems is desirable as it is apparent that some data taken by magnetic oscillographs is not accurate for high-frequency transient phenomena.

Mr. Skeats has asked if there is a co-relation between the recovery voltage data in the MacNeill-Hill paper and the results in the Evans, Monteith, and Witzke paper. We are assured that the two sets of data are in harmony for similar system conditions, and from this we can conclude that the average American grounded-neutral high-voltage system is not subjected ordinarily to extremely high switching voltages and that the more severe voltage transients referred to in the Evans, Monteith, and Witzke paper occur only infrequently and under unusual system conditions. Mr. Prince infers that the De-ion grid shown in this paper is approaching an oil-blast design. Fundamentally the De-ion grid theory is observed in this design; that is, the arrangement of oil pools and splitter plates is essentially the same as on all De-ion grids and the iron is still present to accelerate the arc, particularly on low currents, and to drive it into the restricted slots where deionization is carried out. Grids of this type have been made with and without iron and we feel that the presence of iron improves the grid operation.

Mr. Gay and Mr. Sels of Public Service Electric and Gas Company state that five cycles is not usually necessary for high-voltage circuit breakers, and to this we agree. Public Service of New Jersey, however, is fortunate in having a compact high-power system without marked stability problems. High speed of opening operation, however, particularly when coupled with high speed of reclosure is definitely showing great improvement in stable operation in several outstanding cases. In one such case the stable limit of a 138-kv line was multiplied by three by the installation of circuit breakers which opened and reclosed in 24 cycles. The alternative in that case to such high-speed operation was a parallel line.

Mr. Lott has presented an interesting discussion of Southern California Edison and has pointed out the large number of heavy short circuits which have been handled during their test program without any considerable system difficulty. This in itself is a tribute to the higher speed of breaker operation generally found during the tests, as well as to the ruggedness of the Southern California system and the care with which the system was arranged for the tests.

Mr. St. Clair of the American Gas and Electric Service Corporation has commented on the improvement in modern circuit breakers, both in interrupting ability and in reclosure speeds without departing from the underlying features of standard designs. There are considerable advantages to incorporating these modern features of operation in structures which are known to be adequate for American operating conditions. The operators are thus able to secure equipment familiar to their people and also in many cases to incorporate the high-speed features in old equipment with consequent saving in station construction costs. The general use of grounded neutral systems in America demands more frequent circuit-breaker operation than is the case with ungrounded neutrals, and our first requirement must be the adequacy of the circuit breaker for high-power duty, possibly repeated several times without maintenance, and with a growing demand for instantaneous reclosure.

Transactions

Papers and Discussions Comprising Pages 435–98 of the 1939 Volume

Temperature Aging Characteristics of Class A Insulation

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ASSOCIATE AIEE

J. A. SCOTT
MEMBER AIEE

Synopsis: This paper reviews the literature relating to the effect of temperature on the life of class *A* insulation. It presents test data on aging of built-up samples 100 mils thick, of varnished cloth, both black and yellow, over the temperature range 105 to 200 degrees centigrade.

The effect of temperature on the insulation was measured by three different methods—physical condition by visual observation, bending of the insulation to produce cracking, and the lowering of the breakdown voltage of samples which had been immersed in water. The final results are given in the form of temperature-time curves for several degrees of deterioration as measured by each of the tests.

THE materials used for insulation purposes in electric machines are subject to many factors which determine their useful life. They are subject to the effects of temperature, of mechanical stresses, vibration, electrical stresses, to the effect of oil in oil-filled apparatus, to moisture, dirt, and in some cases corrosive gases. Past experience shows that the life of a machine may be limited by any of these causes, and engineers must make proper allowance for each of them in design.

The importance of the effects of temperature was recognized early and definite limits were agreed upon for the maximum permissible operating temperatures which would constitute good practice for the various types of insulation used. These limiting values were based upon the knowledge of materials available at that time, and the performance of electric apparatus built to conform with these values has shown excellent results.

Since that time the electrical industry has made considerable progress. In addition, the chemical industry has been producing at a very rapid rate new materials which electrical engineers have been carefully studying as promising candidates to supplement or supplant the older forms of insulation.

It therefore is desirable to review briefly the previous work and compare the results in service with expectations on the basis of operating temperatures and with the results of later laboratory tests. Also, from these comparisons it is desirable to examine the latter and determine their significance.

Previous Work

In 1905 a paper by E. H. Rayner[1] in the *Journal* of the Institution of Electrical Engineers discussed the effect of temperature on insulation, pointing out the rapid effects of high temperatures on life. However, no very definite conclusions as to permissible temperatures were drawn.

In 1913 the Institute held a symposium[3] on the subject at which a number of papers were presented and much discussion ensued. The assumption was made that class *A* insulation had a ten-year useful service life at 100 degrees centigrade and one indefinitely long at 90 degrees centigrade, but one of only a few weeks at 125 degrees centigrade. Two ideas will be found in this discussion—one that as high a temperature as possible should be permitted, consistent with reasonable life of the machine, the other the importance of reliability in the operation of the machines and, therefore, the desirability of not departing greatly from the operating temperatures which were actually in use at that time. Following this discussion AIEE Standard No. 1 was evolved which recommended for purposes of standardization the following limiting "hot spot" temperatures for electrical machinery and apparatus:

Class *A* material (treated organic)	105 degrees centigrade
Class *B* material (inorganic, plus binder)	125 degrees centigrade
Class *C* material (pure inorganic)	Not set

In the intervening years laboratory confirmation was undertaken, and a large amount of testing has been carried out by various groups. Experience in the development, design, and performance of apparatus has been a most important factor.

Transformer engineers made aging tests on insulation under oil which have formed the basis of V. M. Montsinger's eight-degree rule.[7] This rule states that the rate of deterioration of oil-immersed varnished cloth doubles for every eight-degree-centigrade increase in temperature. In the same paper Mr. Montsinger states that the rate of deterioration is less for aging in air than in oil, and in his figure 9 gives two dotted curves for air which correspond in slope respectively to 13 degrees and 26 degrees centigrade in-

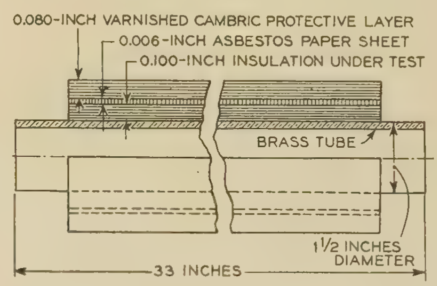

Figure 1. Test sample for aging class A insulation

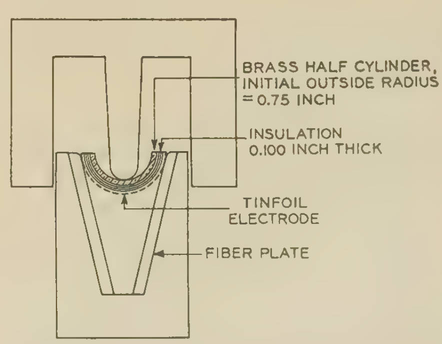

Figure 2. Cracking-on-bending test, showing fixture for bending semicylindrical sample

Paper number 39-7, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 21, 1938; made available for preprinting November 29, 1938.

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1. For all numbered references, see list at end of paper.

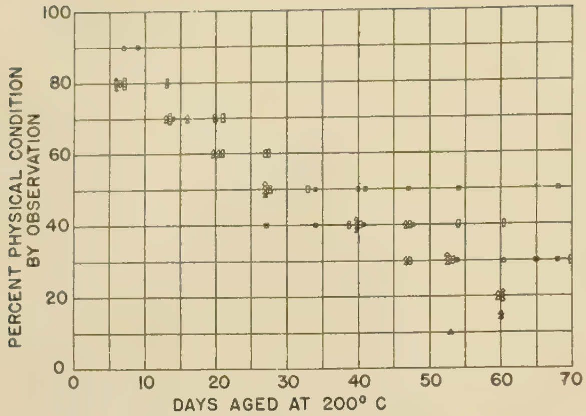


Figure 3

△—Black varnished cloth

⊙—Yellow varnished cloth

⊡—Cotton tape and black varnish

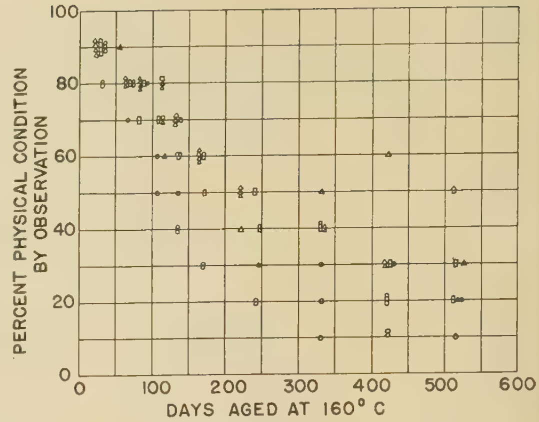


Figure 4

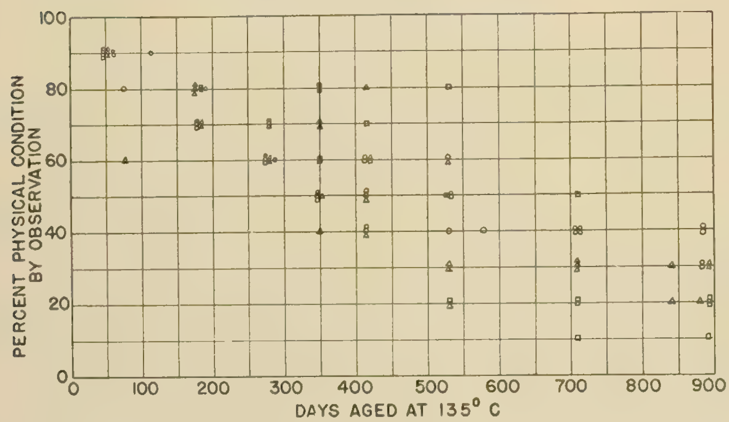


Figure 5

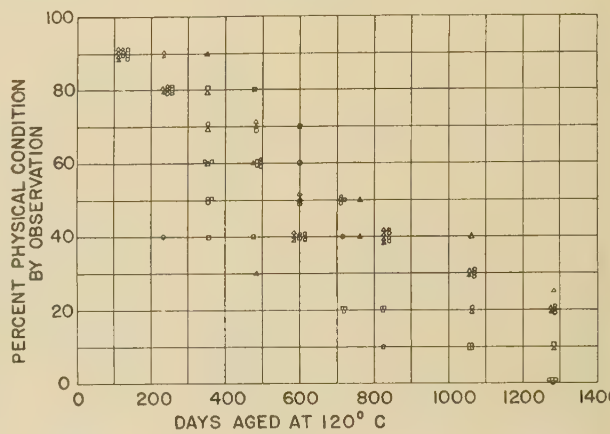


Figure 6

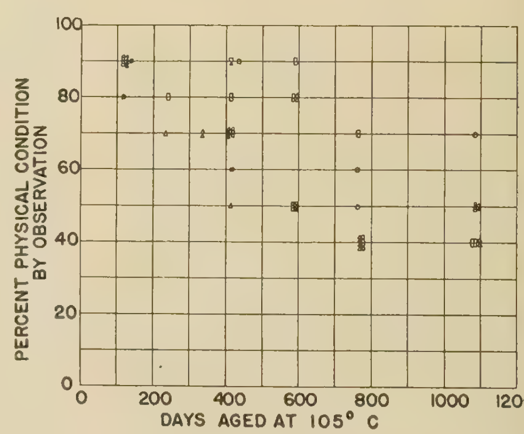


Figure 7

Figures 3–7. Physical condition by observation of class A insulation samples aged for various periods of time at 200, 160, 135, 120, and 105 degrees centigrade in ovens

crease in temperature for half the life.

At Massachusetts Institute of Technology three extensive series of tests[6] on oil-treated cable paper aged in oil as short lengths of cable showed that the mechanical properties deteriorated with time of test, but at temperatures below 90 degrees centigrade a minimum value was reached in 20 to 28 weeks and then a recovery brought the insulation back to nearly the original value. No explanation is offered for this.

A similar problem in the oxidation of oil has led to the development of an interesting technique by Dornte.[12,13,14] The oil is heated in a suitable container and oxygen bubbled through it. The number of cubic centimeters of oxygen absorbed per 100 grams of oil per hour then gives an index of the oxidation rate or aging of the oil at any given temperature. Whitehead[16] has used a somewhat similar method for testing cable oils.

Investigational work carried on in the general engineering laboratory of the General Electric Company during this period is the subject of the present paper. It covers insulation of the type used in rotating machines, namely black and yellow varnished cloth tape and white cotton tape dipped in varnish. Suitable structures with these insulations were aged in air at five temperatures between 100 degrees and 200 degrees centigrade.

Method of Measuring Aging

The criterion for the service life of the insulation of a machine is failure. If this is definitely caused by operation at uniform and long-continued high temperatures, it may safely be said that it gives a measure of the life of that insulation at that temperature for that kind of apparatus and service. In testing the life of insulation in the laboratory it has been impractical to test completed assemblies in the large numbers required to give reliable results. The total number of test specimens used in the present work was 600. Thus it becomes necessary to make tests on the material itself dissociated from the structure. In such tests some definite property of the material is selected and measured periodically after aging at several different temperatures, thus determining for these temperatures a characteristic aging curve for that property. In order to use such laboratory results to estimate the useful life of the insulation under service conditions it is necessary to select some definite deterioration in the measured property as its limit of serviceableness. It is as-

sumed that when it has reached this condition, its useful life is ended.

Properties which may be measured include the following:

Insulation resistance
Electric breakdown
Power factor and dielectric power loss
Physical condition by visual observation
Cracking on bending (radius of bend to produce cracking)
Flexibility
Tensile strength
Folding endurance

The first three are electrical tests and the last five are mechanical tests.

In the present study, tests of the first five types were included. The observations of the physical condition of the insulation (figures 3 to 7) and cracking on bending (figures 8 to 12) show a continuous deterioration due to aging at all temperatures. The electrical properties improved in some cases with aging. For example, the insulation resistance increased as the test progressed, as might be expected due to the evaporation of moisture. The power factor of samples aged at 135 degrees centigrade decreased at first and then increased, as shown in figure 18. The breakdown voltage of the samples tested after immersion in water, given in figures 13 to 17, decreased fairly uniformly over the period of aging.

The criterion used by Montsinger[7] in his work on aging of paper in oil was tensile strength. The resulting curves given in his paper are quite consistent. Folding endurance tests were used in the studies[6] made at Massachusetts Institute of Technology. The results show the insulation aged progressively with time up to a certain point and then recovered. These two latter types of test were not adaptable to the test structure used in the present work, but it is interesting that different observers arrived independently at the greater usefulness of the mechanical test results.

Test Specimen and Aging Procedure

Since it was desired that the tests should typify deeply embedded class *A* insulation as used in motor and generator slots, the test specimen was designed in order to simulate as far as possible such insulation conditions.

Six-hundred samples were used consisting of a 100-mil thickness of the insulation wrapped on a brass tube $1^1/_2$ inches in diameter and 33 inches long, as shown in figure 1. In order to exclude direct contact with the ambient air dur-

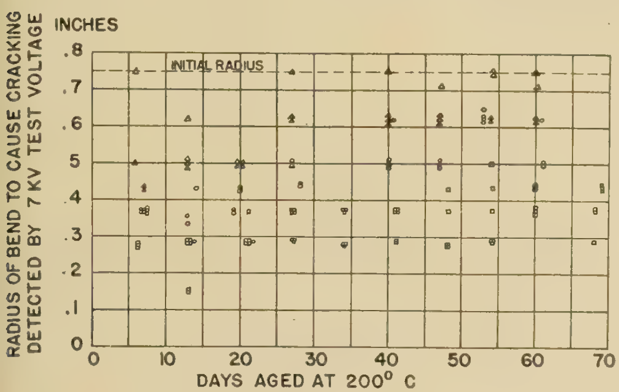


Figure 8

△—Black varnished cloth

⊙—Yellow varnished cloth

⊡—Cotton tape and black varnish

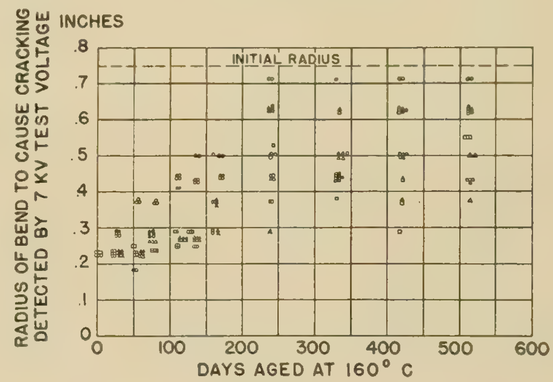


Figure 9

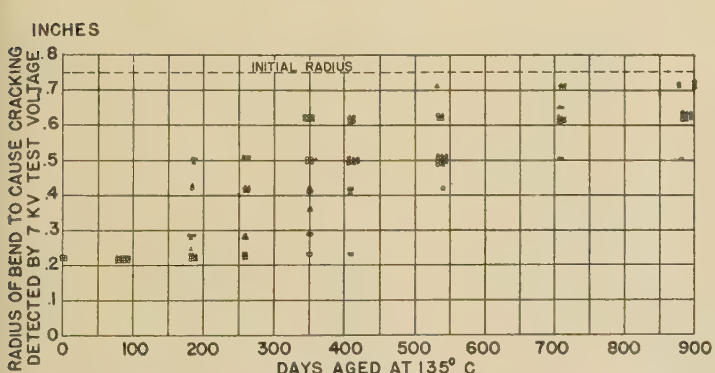


Figure 10

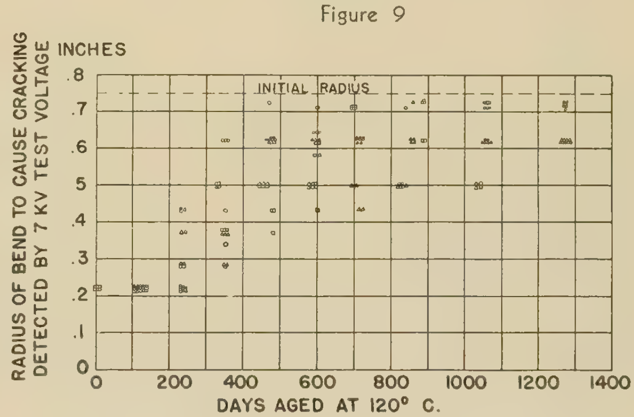


Figure 11

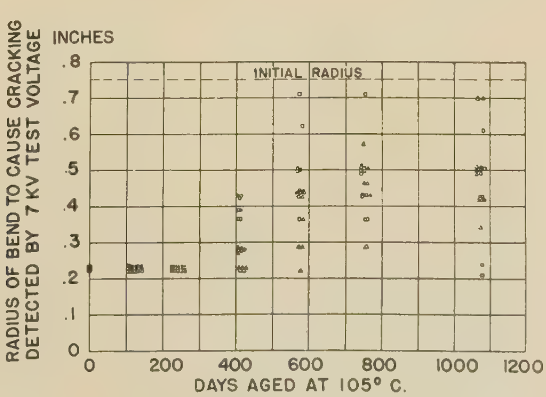


Figure 12

Figures 8–12. Radius of bend to cause cracking detected by seven-kv test voltage of class A insulation samples aged for various periods of time at 200, 165, 135, 120, and 105 degrees centigrade in ovens

ing the aging tests this insulation was protected by a 0.006-inch layer of asbestos paper and an additional 0.080-inch layer of varnish tape. The purpose of such protection was twofold. First, it was considered that in an actual machine the outer layers of insulation are the coolest and serve to protect the inner layers which, being the hottest, age most rapidly. Second, for laboratory tests it was desirable that the insulation age as uniformly as possible throughout its mass in order to get consistent results which might in turn be used with

other data to allow for the difference in temperature of the various layers or other conditions in an actual machine.

This choice of deeply embedded insulation and the fact that the tests were carried out in air restricted the type of test criterion. For example, tensile strength or other similar mechanical tests could not be made on the aged tapes since the entire thickness of insulation hardens during aging into a dry mass that will not permit the tapes to be unwound without tearing.

Three kinds of insulation were used in these tests:

A. Black varnished cloth tape
B. Yellow varnished cloth tape
C. White cotton tape treated by successively baked dips of black varnish

The tube samples were placed in ovens and held at the following temperatures: 200, 160, 135, 120, and 105 degrees centigrade. Once each week the heat was shut off for six hours and the oven doors opened to allow the samples to cool to room temperature and undergo the expansion and contraction that is encountered in actual operation in machines.

Samples were taken from the oven according to the schedule listed in table I. The 200-degree-centigrade tests were completed in about 2 months, the 160-degree-centigrade tests ran 18 months, while the tests conducted at 120 degrees centigrade extended almost 4 years.

Electrical Tests

The electrical tests of insulation resistance, breakdown voltage, and dielectric loss and power factor were made in the usual manner on (*a*) dry tubes as taken from the oven and cooled to room temperature, (*b*) wet tubes, soaked in tap water for 48 hours on removal from the oven.

Mechanical Tests

The mechanical tests were physical condition by visual observation, and cracking on bending.

The tests for physical condition were made by a personal observation of four samples at each test period. An expert can estimate the degree of deterioration very closely by the general appearance, breaking and rubbing small pieces with the fingers, scratching with thumb nail, etc. The chief objection to this method is the personal element involved. Where samples of different types are to be compared and classified, there is an unconscious tendency to favor those types which the examiner had previously believed to be the best. It is also very difficult to classify correctly a large number of samples unless some systematic method is followed.

With this in mind a system of classification was devised. It aims toward eliminating the personal element as far as possible and reducing the results to the simplest terms. Samples were not selected for examination in the order of

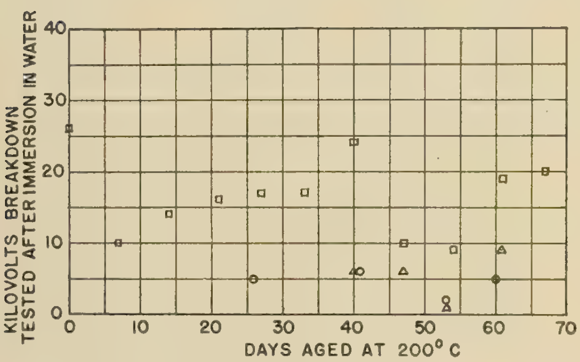


Figure 13

△—Black varnished cloth
⊙—Yellow varnished cloth
⊡—Cotton tape and black varnish

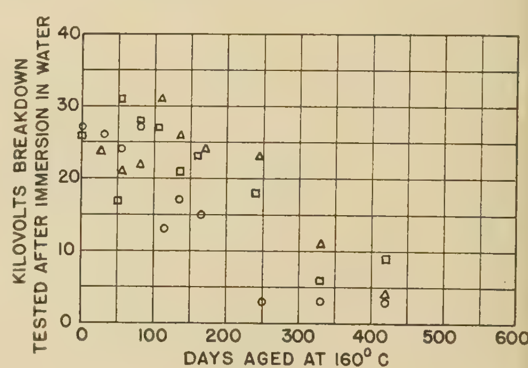


Figure 14

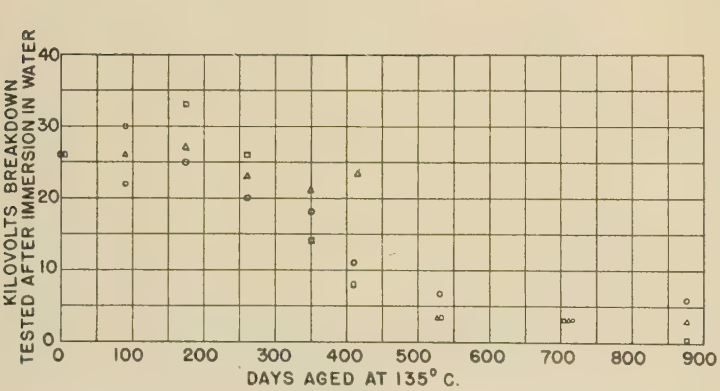


Figure 15

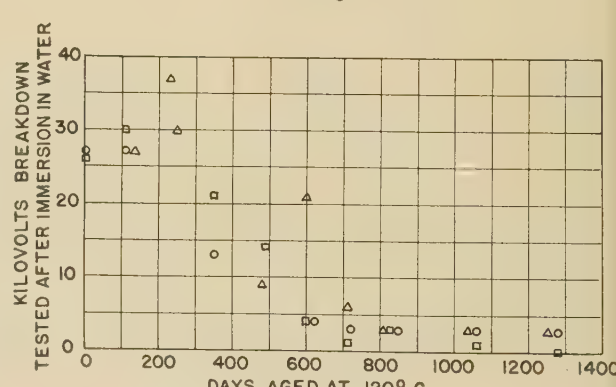


Figure 16

Figures 13–17. Kilovolt breakdown of class A insulation samples 100 mils thick aged for various periods of time at 200, 165, 135, 120, and 105 degrees centigrade in ovens. Tested wiped dry after immersion in tap water for 48 hours

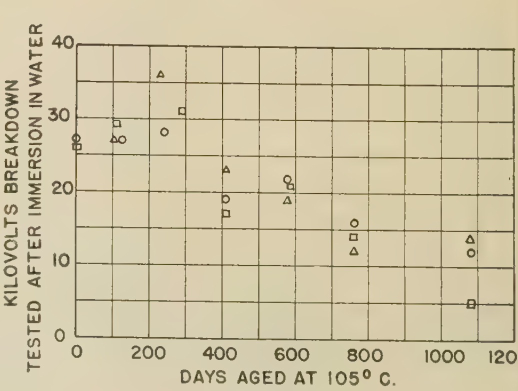


Figure 17

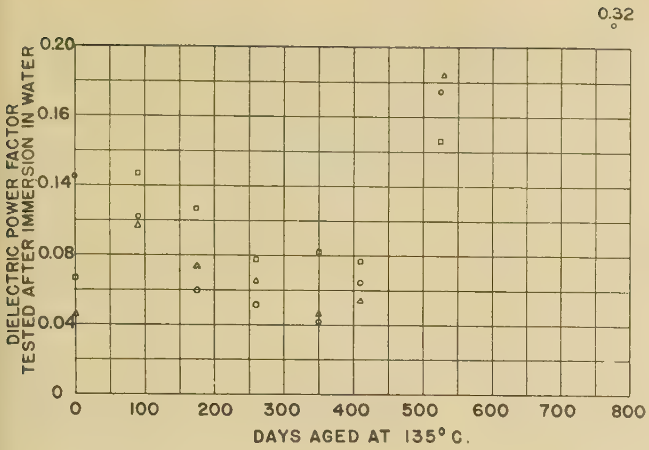


Figure 18. Dielectric power factor of class A insulation samples aged for various periods of time at 135 degrees centigrade in ovens. Tested wiped dry after immersion in tap water for 48 hours

△—Black varnished cloth
⊙—Yellow varnished cloth
⊡—Cotton tape and black varnish

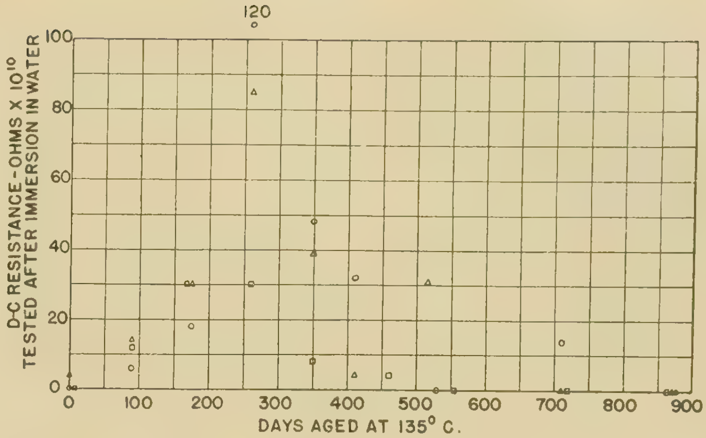


Figure 19. Insulation resistance of class A insulation samples aged for various periods of time at 135 degrees centigrade in ovens. Tested wiped dry after immersion in tap water for 48 hours

△—Black varnished cloth
⊙—Yellow varnished cloth
⊡—Cotton tape and black varnish

their aging period or type, but were selected at random. The "lining up" of the classification or rating with length of aging time then furnished a good check on the judgment of the examiner. The classification was as follows:

Per Cent Rating	Physical Condition by Visual Observation
100	Fresh, soft, and flexible (still tacky)
90	Dry but still soft and flexible
80	Dry but still flexible (hardening)
70	Dry and hard but still retaining flexibility
60	Hard and compact with very slight flexibility
50	Hard, compact, and inflexible but not exactly brittle
40	Brittle but compact and without checks
30	Brittle with checks forming
20	Brittle, checked, and partly cracked (slightly crumbly)
10	Badly cracked and crumbly (partly charred)
0	Completely cracked, charred, and crumbly

The above system of rating applies only to samples protected from exposure to air during the aging process by protective wrappings, which are removed before final tests are made. The total thickness of such insulation samples deteriorates uniformly and closely represents the inner wrappings of coil insulation where the "hot spot" occurs. When samples are exposed directly to air in an oven, the outer wrappings deteriorate faster than the inner, and a percentage rating would be very difficult to make.

Table I

Temperature at Which Samples Were Aged (Deg C)	Time in Days at Which Tests Were Made
200 (run 1)	7, 14, 20, 27, 40, 47, 54, 61
200 (run 2)	8, 14, 20, 27, 34, 41, 47, 54, 61, 68
160	27, 54, 81, 108, 136, 169, 243, 331, 419, 514
135	88, 176, 263, 350, 413, 531, 707, 877
120	114, 236, 350, 479, 600, 714, 825, 1,053, 1,283
105	114, 236, 413, 586, 761, 1,085

The test to determine the amount of cracking on bending was as follows: Six good half sections were cut from the tube samples for bending test. These were seven inches long and semicircular in cross section and included both the thickness of insulation to be tested and the section of brass tubing directly under it, having a normal radius of 0.75 inch. Using vaseline as a paste, a strip of metal foil was applied five inches long and three-quarters-inch wide in the center of the outer surface area. The section of brass tubing served as the ground electrode and the strip of foil as high-voltage electrode in the test which followed. The sample was placed in a bending machine (see figure 2) and bent from the normal tube radius of 0.75 inch until it coincided with a templet having a minimum radius of 0.71 inch. Holding the sample at this radius, seven kv* was applied for one minute. If the sample did not fail, it was bent to the shape of a second templet having a radius of 0.62 inch and seven kv again held for one minute. Continuing in steps with templets having minimum radii respectively of 0.50 inch, 0.43 inch, 0.37 inch, 0.29 inch, and 0.22 inch until sample failed at seven kv, a radius was determined which produced cracking. Five additional samples were tested and the results averaged. The radius at which this cracking occurred was used as the index of cracking on bending.

* The normal breakdown of these tubes when new would be 30 kv approximately.

Results of Tests

As mentioned before, three kinds of insulation were tested:

A. Black varnished cloth tape
B. Yellow varnished cloth tape
C. White cotton tape treated by successively baked dips of black varnish

To secure a more reliable trend the data for all three kinds of insulation tested, namely black varnished cloth, yellow varnished cloth, and cotton tape treated with varnish, were consolidated. As these are all of the same general class there was more advantage to be gained by combining the data than to attempt separate analysis. However, in all the figures these materials are indicated by separate codes, triangles, circles, and squares respectively representing black varnished cloth, yellow varnished cloth, and cotton tape treated with black varnish.

The complete plots of physical condition are presented in graphic form as follows:

Figure 3. Physical condition–days aged at 200 degrees centigrade
Figure 4. Physical condition–days aged at 160 degrees centigrade
Figure 5. Physical condition–days aged at 135 degrees centigrade
Figure 6. Physical condition–days aged at 120 degrees centigrade
Figure 7. Physical condition–days aged at 105 degrees centigrade

These curves show a definite reduction in the physical condition by observation

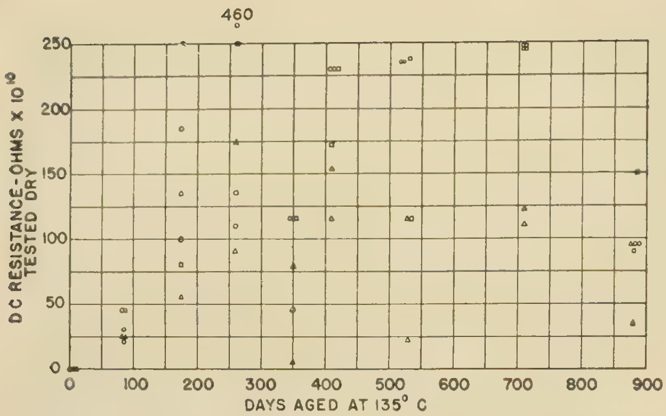


Figure 20. Insulation resistance of class A insulation samples aged for various periods of time at 135 degrees centigrade in ovens. Tested dry

△—Black varnished cloth
⊙—Yellow varnished cloth
⊡—Cotton tape and black varnish

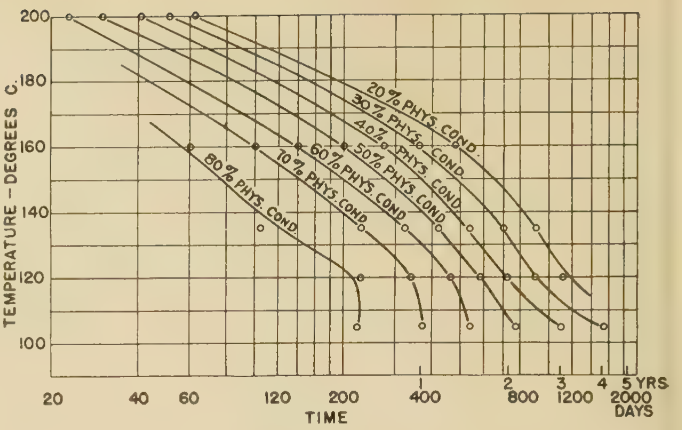


Figure 21. Curves showing the relation for class A insulation between temperature and time of aging to produce various physical conditions as determined by observation

as the aging progresses. Also, the higher the temperature the more rapid the deterioration to any given condition and hence the shorter the life. While the points are scattered, fairly representative average values can be obtained on account of the large number of samples used. The intersection of these average curves with the ordinate for any per cent physical condition gives the time required for the insulation to reach that condition at that temperature. It should be noted, of course, that the per cent physical condition represents the arbitrary evaluation previously given and there is not necessarily the definite relation between the conditions which the numbers would indicate. Nevertheless, on account of the number of conditions used, any resulting errors tend to be minimized as may be seen from the curves.

The complete plots of the results of the cracking on bending test are presented in graphic form as follows:

Figure 8. Radius of bend–days aged at 200 degrees centigrade

Figure 9. Radius of bend–days aged at 160 degrees centigrade

Figure 10. Radius of bend–days aged at 135 degrees centigrade

Figure 11. Radius of bend–days aged at 120 degrees centigrade

Figure 12. Radius of bend–days aged at 105 degrees centigrade

These curves show that when nearly new, the insulation wall may be bent from its initial radius of 0.75 inch to almost 0.2 inch before cracking occurs for an aging temperature of 105 degrees centigrade, but that when aged upward of 1,200 hours, it may crack when deformed only slightly to 0.71 inch. At 120 degrees centigrade the upward drift of the radius at which cracking occurs is more clearly shown as at this temperature the test time was long enough to show definitely the life by cracking. The other higher temperatures show similar trends. A comparison of the curves shows that the higher the temperature the shorter the time to cause cracking at any given radius of bend and thus the shorter the life. The points in these diagrams are also scattered, but again fairly representative values can be obtained due to the large number of samples used. The results at 200 degrees centigrade show quite a spread and emphasize some of the difficulties attendant on too highly accelerated tests carried out at extreme temperatures.

The complete plots of the results of kilovolt breakdown taken on samples previously immersed 48 hours in water and then wiped dry are presented in graphic form as follows:

Figure 13. Kilovolts breakdown–days aged at 200 degrees centigrade

Figure 14. Kilovolts breakdown–days aged at 160 degrees centigrade

Figure 15. Kilovolts breakdown–days aged at 135 degrees centigrade

Figure 16. Kilovolts breakdown–days aged at 120 degrees centigrade

Figure 17. Kilovolts breakdown–days aged at 105 degrees centigrade

This group of curves shows in general a gradual decrease in breakdown voltage as the period of aging is increased, especially in the case of the samples aged for the longer times at the lower temperatures.

Typical data on insulation resistance and power factor are shown in:

Figure 18. Power factor–days aging at 135 degrees centigrade

Figure 19. Insulation resistance–days aged at 135 degrees centigrade (after immersion in water)

Figure 20. Insulation resistance–days aged at 135 degrees centigrade (dry)

The insulation resistance and power-factor test data were of such a nature that the dependence of the properties on the aging period was not so clearly defined as in the tests for physical conditions, cracking on bending, and kilovolts breakdown after immersion in water. For the purposes of this presentation the discussion has been confined to the results obtained in these last three types of test. Thus, although a complete series of tests was made for each different temperature, only a set of curves taken at 135 degrees centigrade is presented to illustrate the general tendency of the insulation resistance and power factor.

Aging Curves at Various Temperatures

The data on physical condition, cracking on bending, and breakdown voltage after immersion in water were reworked into the form of aging curves at various temperatures.

In the case of physical condition the procedure was as follows: On figures 3 to 7 average curves were drawn through points of average physical condition for a given time of aging. From these average curves the times required for the insulation to reach selected values of physical condition were read. These times were then plotted against the temperature of aging, yielding the curves shown in figure 21.

The procedure for the cracking on bending data was similar. In figures 8 to 12 inclusive average curves were drawn and the time noted at each temperature to reduce the sample to a

condition in which it would have cracked if distorted to a given radius. The resulting curves, figure 22, give the days aging for each temperature to produce cracking on bending for four radii of bend.

For the breakdown-voltage curves the measure of the aging was taken as the time for the breakdown voltage to decrease to arbitrarily selected values. In figures 13 to 17 inclusive, average curves of voltage were drawn and the time estimated at each aging temperature for the breakdown voltage to be reduced to 10 kv and 20 kv. The times required to cause the samples to fail at these voltages are plotted in figure 23.

Figures 21, 22, and 23 summarize the results of these aging tests. They represent the aging time at various temperatures to reach certain conditions of deterioration. They indicate that up to 200 degrees centigrade there is no critical temperature above which the insulation suddenly fails, but that for each temperature there is a definite time required for the insulation to reach a given condition due to heat aging, and that in general the lower the temperature the longer the life.

Comparison of Foregoing Results

For the purpose of comparing the results obtained by the three different methods, that is, physical condition by visual observation, cracking on bending, and kilovolts breakdown, figures 24 and 25 were prepared. The former shows, plotted together, the curves for 60-per-cent physical condition, 30-per-cent physical condition, 0.6-inch radius to produce cracking on bending, and 10-kv breakdown. Figure 25 shows the same data plotted on a logarithmic time scale. It will be noticed that, although each of these three curves has a different basis of selection, they have roughly similar shapes.

The underlying causes of the changes of shape in some of these curves, especially in the region between 110 and 140 degrees centigrade, present an interesting field for study. One of the first effects of heat is to drive off moisture. Continued application of heat may result in a direct chemical change in the molecules of the organic material itself, or a change due to oxidation in the presence of the atmosphere which might not occur in an inert atmosphere. Further changes may then result due to by-products of the oxidation process. To make a scientific study of the temperature aging of insulation these and other variables would require separate investigation. When the variables in the materials themselves are considered as well, it is evident that life-test results must be interpreted with caution.

Discussion of Results

After years of labor involving the most careful preparation of hundreds of test specimens, the maintenance of these at constant temperature in ovens provided with proper temperature distribution for the purpose, the testing of these after aging for various properties, the tabulation of thousands of results, we finally arrive at summary curves as shown in figures 21 to 23.

Some authors[7,11] have proposed rules for calculating the rate of deterioration of insulation as a function of the temperature, such as that the life of the insulation is reduced one-half by each ten-degree-centigrade rise in the operating temperature. The results of the present tests indicate that while such rules may furnish approximations for purposes of simplification in practical applications, due care must be taken to allow for the difference between the actual materials and structures used in the laboratory tests and those of the apparatus under operating conditions. In addition, since the curves show that the rate of deterioration is not constant, too great reliance should not be placed upon short-time tests alone, but they should be supplemented by long-time tests.

At the beginning of the investigation it had been hoped that the work of obtaining such a curve would result in a standard of reference so that as new materials became available they might be tested at higher temperatures to obtain their aging characteristics in a relatively short time, and if these appeared to be outstanding, further investigational work would be carried on toward a more complete evaluation of the material at the lower temperatures. To some extent this objective has been attained, although considerable work is still involved in such tests.

As soon as results are obtained from laboratory tests of any kind there is the immediate urge to correlate them with practical results, and so it has been with these tests. The first reaction is probably a surprise as to the shortness of life as compared with the long life of apparatus in service, but when it is considered that these values are the result of continuous aging at temperatures 105 degrees centigrade and higher, the results are not so startling. In fact, where opportunity has arisen to compare these results with the life of insulation in practice, which was known to have been operated substantially continuously at a given temperature, the correlation has

Figure 22. Curves showing the relation for class A insulation between temperature and time of aging to produce cracking by bending to various radii of curvature

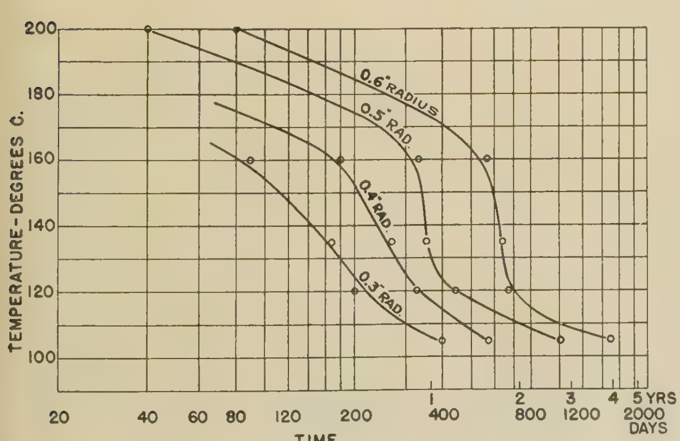
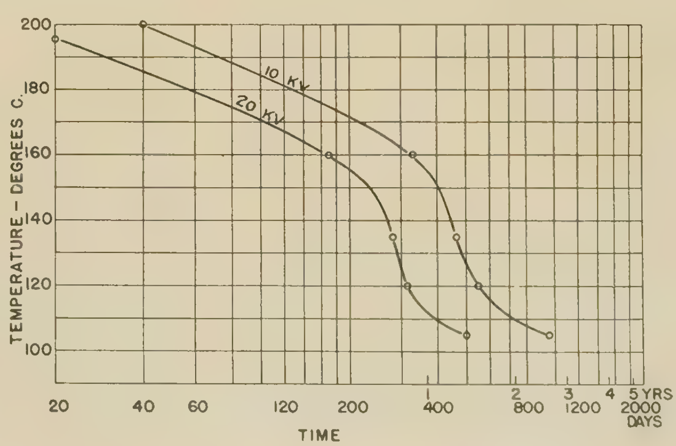


Figure 23. Curves showing the relation for class A insulation between temperature and time of aging to reduce the kilovolts breakdown tested after immersion in water for 48 hours to 10 kv and 20 kv for 100-mil thickness of insulation



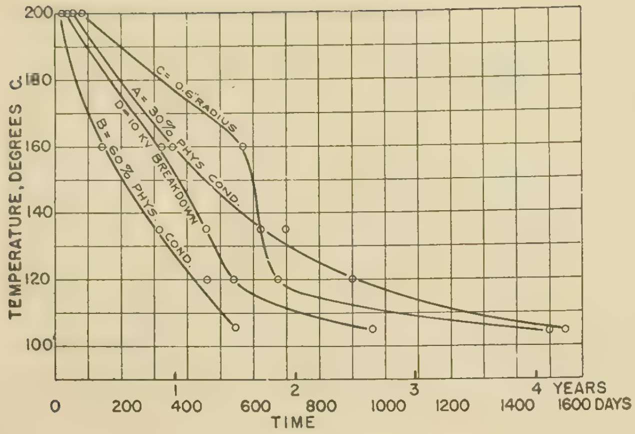


Figure 24. Aging curves of class A insulation, showing the relation between temperature and time of aging

A—Aged to 30-per-cent physical condition

B—Aged to 60-per-cent physical condition

C—Aged to crack on bending to 0.6-inch radius

D—Aged to ten-kv breakdown after immersion in water

Arithmetic scale

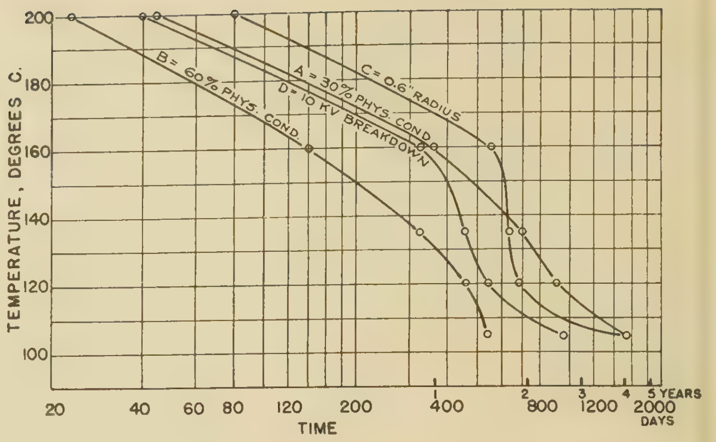


Figure 25. Aging curves of class A insulation, showing the relation between temperature and time of aging

A—Aged to 30-per-cent physical condition

B—Aged to 60-per-cent physical condition

C—Aged to crack on bending to 0.6-inch radius

D—Aged to ten-kv breakdown after immersion in water

Logarithmic scale

been such as to indicate reasonable agreement. Probably the greatest benefit of these aging tests has been to allow design engineers to see the effect of continuous operation at elevated temperatures upon the insulation and to observe the shortness of the resulting life.

Another result of this investigation has been to show the reasonableness of the 105-degrees-centigrade hot-spot temperature for purposes of standardization and for reference since it is quite plain that above this temperature deterioration increases rapidly under continuous operation, whereas below it the indications are from an extrapolation of the curves that the life is long. This value therefore represents the results of engineering judgment based on technical knowledge to obtain optimum service so that full use will be obtained of the insulation strength and life. The allowance for various service conditions is then regulated by the temperature rise measured during the rating test of the apparatus.

New Materials

The advances in bringing forth many new materials in the past years have given to design engineers opportunity for obtaining increased performance, and yet it is probably safe to say that for cellulose materials there has been no fundamental increase in temperature limit. Certain new synthetic materials give opportunity for some increase which can be used to advantage, and it may be that work should be undertaken to classify these materials more directly. It may be satisfactory for their temperature limit to be placed somewhat above that of class *A*, but they surely are not class *B*.

Conclusions

A method of testing the aging of insulation at various temperatures between 105 degrees centigrade and 200 degrees centigrade using carefully constructed laboratory samples has been described. A large number of samples was used. Three different methods of tests were applied. Each method was successful in indicating progressive deterioration of the samples during aging, and the agreement between the results obtained by the three different test methods is also reasonably good. Thus it appears that the methods used may be applied with confidence to the further evaluation of new materials.

The purpose of presenting this material at this time in connection with other papers bearing on the rating of motors run on interrupted duty is to give available data on the effect of temperature on life and to bring forth in discussion from others the data and experience they may have relating to this problem.

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Discussion

J. L. Rylander (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The paper by J. J. Smith and J. A. Scott is a very desirable addition to the

subject of insulation. Little test data of this kind had been obtained and seldom has any of it been published.

These data are very useful in the consideration of the permissible operating temperatures of electrical machines with class A insulation. However, I wish to draw attention to some other features that must be taken into the consideration of safe operating temperatures of machines with class A insulation.

It should be noted that these are electrical and mechanical tests on the insulating materials themselves, but not on the motors or generators or even complete windings or coils themselves.

The matter of the design of the windings and all insulation details have a marked effect on the life of the windings at higher temperatures. The weakest detail of insulation determines when the winding fails.

The effect of any temperature above the boiling point of water has a decided effect whenever the insulation has absorbed moisture.

The shrinkage of insulation will often determine the life of many motors and generators. This is most noticeable where there are strong mechanical stresses in the windings due to either centrifugal force or to the stresses due to very heavy currents when starting or at any other time. Shrinkage is noticeable when all of the moisture content is removed from the insulation and this occurs when the boiling point of water is reached.

As stated in the paper there are no formulas for determining the approximate life of windings at various temperatures, but a couple empirical formulas are referred to as having been previously suggested by others. I developed the following formula for my own use from all data that has been available:

$$\text{life} = \frac{K}{(T - A)^2}$$

where K is practically a constant but depends on the general construction or type of winding and T is the operating temperature in degrees centigrade. A is practically a constant but depends upon the kinds and grades of the insulations used. The writer usually uses the value 83 for A. This formula indicates that if you want a motor with class A insulation to operate for a very long period such as a hundred years, the maximum temperature would be about 83 degrees centigrade.

P. L. Alger (General Electric Company, Schenectady, N. Y.): There are two highly significant trends in recent insulation developments.

First, there is the development of a multitude of new synthetic materials, whose composition and manufacture are carefully controlled from raw material to finished product. These include the chlorinated hydrocarbons, or Pyranols, for transformers and capacitors, Formex, Glyptal, and other new enamels and varnishes, fiber glass for high temperature insulation, and a vast number of new resins or plastic compounds.

Second, there is the growing use of controlled atmospheres for industrial operations and especially for electrical apparatus. These include hydrogen for rotating machines, nitrogen for transformers and cables, carbon dioxide for explosion-proof motors, sulphur dioxide and Freon for hermetic refrigerator motors, and other gases for high-voltage apparatus.

Both of these trends bring the chemist into the picture, and force the electrical engineer to study chemical reactions, rather than merely physical changes in insulation. There is a basic law of chemical reaction which requires that all chemical changes take place at an accelerated rate as the temperature increases, the rate being approximately double for every ten-degree increase in temperature.

It is, therefore, evident that our future progress will be largely determined by how effectively we bring chemistry to bear on our insulation problems. We must initiate a broad program for development of new insulating materials by chemists. We must ask the chemists to devise new methods of accelerated life tests for these new materials. And, we should ultimately revise our basic standards of temperature rating of apparatus to enable these new materials to be utilized to the fullest degree.

V. M. Montsinger (General Electric Company, Schenectady, N. Y.): I am very much interested in this paper as the authors' work indicates that the rate of change of aging is not constant over the entire range of temperatures from 105 to 200 degrees centigrade. In other words, instead of the rate doubling for a definite number of degrees increase in temperature, it requires an increase of some 20 to 30 degrees in the lower range, and some 10 to 15 degrees in the higher temperature range to double the rate of aging.

It can readily be seen that heavier overloads could be allowed by the curves given in this paper than would result from using the eight-degree rule which we have used in the past.

Until recently, so far as I know, with the exception of L. C. Nichol's work (reference 9), no attempt has been made to estimate to what extent short-time heavy overloads use up the life of the insulation. If a changing rate of aging versus temperature (as indicated in the paper) is used, it becomes practically impossible to integrate the time-temperature areas for short-time overloads. Even when using a simple rule like the eight-degree rule it is no easy task, for the reason that it is difficult to calculate the hottest spot temperature and then to resolve the time-temperature areas into forms suitable for integration.

In most types of rotating machinery, heavy overloads usually are not limited by temperature but by other factors like torque, stalling, commutation, etc. Where the amount of overload is limited for electrical reasons to moderate values, it appears that the effect of overload on the aging of the insulation can be closely approximated by using some simple method like the eight-degree rule.

Last year I had an opportunity to analyze short-time heating conditions applying to neutral grounding devices which are called upon only under line fault conditions to function under load, when fairly high temperatures may be reached for time periods ranging from one to ten minutes or more. By integrating the temperature-time area and using the eight-degree rule, temperature limits were approximated which were conservatively safe.

Where the aging of the insulation changes at a constant rate, that is, where it doubles for each 8, 10, or 12 degree increase in temperature, the temperature-time area can be integrated to obtain the amount of aging per cycle by the following equation:

$$A = t\left[\frac{\epsilon^{XT_2} - \epsilon^{XT_1}}{X(T_2 - T_1)}\right] \tag{1}$$

where

A may be designated as aging units.

t = time, per cycle
ϵ = 2.718
T_2 = maximum temperature
T_1 = initial temperature
X = constant
= 0.088 when aging doubles for each 8-degree increase
= 0.0695 when aging doubles for each 10-degree increase
= 0.059 when aging doubles for each 12-degree increase

It is usually more convenient to use a formula that gives the per cent of life used up per cycle of operation. This can be done by dividing the aging per cycle equation 1 by an assumed life at a given constant temperature.

For estimating the life of insulation as determined by tensile strength, I have used the following formula:

$$Y = 7.15 \times 10^4 \epsilon^{-0.088T} \tag{2}$$

where

Y = life in years
T = temperature in degrees centigrade

It is well to point out that the laboratory tests reported in my 1930 paper (reference 7) indicate that the constant in equation 2 should be 4×10^4 which would give a life of four years at 105 degrees centigrade. My experience, however, has been that we should depend mostly on laboratory tests on small samples of insulations to give us the relative "rates of aging" at different temperatures, but that the life of transformers can best be determined by life tests made on actual transformers. Both laboratory aging tests on transformers and field experience have shown that the actual life of transformers operating at approximately 105 degrees hot test spot is more nearly like seven years than four years.

According to equation 2 the life of insulation is gone at the end of approximately seven years operation at 105 degrees centigrade. This is in fair agreement with the figure 21 which shows that the insulation has from 20 to 30 per cent of its strength left after operating from four to five years at 105 degrees centigrade. It is at the higher temperatures that the agreement is not so good because the curves in figure 21 show that the temperature must be increased more than eight degrees to double the rate of aging.

While the eight-degree rule may not be rigidly correct, particularly for high temperatures, I prefer to use it since it is more conservative than the data shown in figure

21. Even when using the eight-degree rule, one will be surprised at the large number of times that insulation can be subjected to short-time overloads, without using up its life.

If the eight-degree rule is used, and if equation 1 is divided by the constant in equation 2, and the number of hours per year, the following equation results:

$$F = \frac{t(\epsilon^{0.088T_2} - \epsilon^{0.088T_1})}{7.15 \times 10^4 \times 24 \times 365 \times 0.088(T_2 - T_1)}$$

$$= \frac{t(\epsilon^{0.088T_2} - \epsilon^{0.088T_1}}{K(T_2 - T_1)} \qquad (3)$$

where

F = fractions of life used up per cycle
K = 55.1×10^6 for time t expressed in hours per cycle

If t is expressed in minutes per cycle $K = 330.6 \times 10^7$.

Charles F. Hill (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The method of taking data by visual observation, introduced by Smith and Scott, is of considerable interest in the study of insulation deterioration. These observational data are quite consistent, even more so than the actual physical measurements, but are arbitrary in that the various six or eight stages of deterioration are established by mere definition. It would seem necessary to have simultaneous physical measurements before much confidence could be placed in such inspection data, but it is my opinion that such observations are quite worth while.

There is a considerable discrepancy between the physical data and observational data by Smith and Scott as is shown in figure 25 of their paper in the interval of 400 to 800 days operation where the mechanical data dips sharply. I am wondering if this dip is not due to some accidental phenomenon in the test.

Comparing the general rate of deterioration between the data by Smith and Scott and my own data ("Temperature Limits Set by Oil and Cellulose Insulation," AIEE TRANSACTIONS, volume 58, 1939, pages 484–91), it would appear that the rate in oil with a rather inert gas present is slightly faster than that for the test in restricted air by Smith and Scott. This is a surprising result, although not so impossible as our inert gas was not 100 per cent oxygen free. Small amounts of oxidation products of the oil might have a serious influence on the varnished cellulose materials.

In the discussion of results, Smith and Scott have reached a conclusion which, I believe, merits further emphasis. During the past few years, it has become customary for insulation engineers to speak of insulation deterioration rates doubling for each ten-degree-centigrade temperature rise. This idea has arisen because of the fact that in chemical reactions, rates of reaction do tend to change by a constant factor for constant temperature intervals. This is under the assumption, however, of constant materials and constant conditions of concentration, etc. Smith and Scott point out that such a rule does not hold for their life tests. We have also reached the same conclusion. The reason for the failure of this chemical law to hold for insulation life tests lies chiefly in the fact that in life tests, the material being tested is not the same material after a few weeks or months. The result is a very different rate of deterioration after some months than would be expected if the material remained a constant factor. Smith and Scott point out the danger from short-time tests. I would like to emphasize this particularly in that extrapolating rates from short-time tests may be very misleading.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): This paper is extremely interesting and adds considerably to our knowledge. A few points that strike me are as follows:

1. Results of the tests tend to scatter considerably, as has been found by practically all investigators making any sort of comprehensive study.

2. A relatively short life will prevail for class A insulation operated continuously at the present "hot spot" temperature limit of 105 degrees centigrade of the AIEE rules. In my paper at this convention on "Load Ratings of Cable" (see 1939 annual TRANSACTIONS index for page numbers) I have indicated that for continuous operation it seems best from the standpoint of obtaining long life to have maximum temperatures not exceeding 85 or 95 degrees centigrade depending on how "continuous" the operation is at the higher temperatures.

3. If class A insulation is operated in normal service as is usually done, that is, at temperatures considerably below 105 degrees centigrade, then it becomes feasible to operate during emergencies at temperatures in excess of 105 degrees centigrade, presuming that such emergencies are infrequent and the temperature of the insulation is at the highest values for only a few hours during each emergency.

4. The operation during emergencies appears to do little more harm if the maximum temperature is around 140 than if it is around 120 degrees centigrade according to figures 22 and 23. In view of these unusual findings, it appears that the conclusions in my paper about operating low-voltage cables during emergencies at temperatures up to 120 degrees centigrade may be conservative.

In a series of six-month tests made at 100 and 125 degrees centigrade in Chicago on varnished cambric insulated cables, it was found that the rating of the quality of the various insulations as determined by mechanical and electrical tests on insulation removed from sealed cables was different from the rating found on the same insulations when tested in tape form in air. When heated as tapes exposed to air, the materials first hardened and improved in tensile and electrical strength but became more brittle. Some materials which appeared to be superior in such tests were found to become stuck together when heated in cable where air is excluded so that the cable could no longer withstand much bending without breaking the insulation.

J. J. Smith and **J. A. Scott**: We have endeavored to point out in the paper and it is again emphasized by Mr. Rylander that there are other factors in addition to the effect of temperature to be taken into consideration in determining the safe operating temperature of a machine. However, it is desirable to study these effects one at a time and therefore the present work was limited to the effect of temperature alone on the aging of insulation. We note Mr. Rylander's simple form of equations for life and it would be interesting if he added the value of K for some typical applications.

Mr. Alger points to the introduction of many new materials in recent years. The introduction of Formex wire is an example of such a development. As more of these materials become available, testing will be required to evaluate their performance compared with the older materials and the paper suggests a technique which may be used for this purpose.

Mr. Alger and Mr. Hill discuss the bearing of chemical changes on the aging problem and refer to a law the chemist often finds that the rate of change doubles for each 10-degree-centigrade increase in temperature. Such a law frequently holds for a single definite chemical reaction, and we would have been pleased if it had turned out that way in the present tests, but it did not. As pointed out in the paper, the consideration of the aging reactions from a physical and chemical standpoint as well as the effect of the structure itself is a subject worthy of more detailed study.

Mr. Montsinger's eight-degree rule is quite widely accepted in transformer practice and from all the evidence is conservative, as he points out when used to estimate the effect of increases in temperature. However, when the effect of decreases in temperature is considered as, for example, in estimating the service life of materials from short-time laboratory tests at elevated temperatures, the eight-degree rule may indicate a much longer life than is actually shown by these tests. In other words, the available evidence indicates that the actual rate of aging is not a simple law, but is dependent on many individual circumstances, so that the only safe basis for conclusions is actual operating experience.

Doctor Hill asks if some accidental phenomenon might account for the difference in the shape of the curves of physical and observational data in figure 25 between 400 and 800 days. These results were checked back to the original data and confirmed.

Doctor Hill points out that the aging rate he found in oil with inert atmospheres was faster than the rate in air given in our tests. Montsinger in his 1930 AIEE paper "Loading Transformers by Temperature" reports a similar behavior. This question of inert atmospheres is a fruitful field for future work, with the extended use of hydrogen cooling and other enclosed types of motors.

We are interested in Mr. Halperin's conclusion from work in connection with cable that the life of insulation operated continuously at the present "hot spot" temperature limit of 105 degrees centigrade is relatively short and his indication of a continuous temperature not in excess of 85 to 95 degrees centigrade. With regard to his discussion of higher operating temperatures in item 4 it should be remembered that the structure used in our tests is not the same as that in which he is interested and thus the results may not be directly applicable. His description of the results of aging tests on cable insulation both when sealed and aged in air indicate the necessity for care in comparing the results of aging tests made under different conditions.

Rating of General-Purpose Induction Motors

P. L. ALGER
FELLOW AIEE

T. C. JOHNSON
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Synopsis: This paper reviews principles of rating of general-purpose a-c motors, particularly in relation to overload torque and temperature limits.

The overload capacity of an electric motor is limited, first, by its stalling, or breakdown torque, and, second, by its operating temperature. The breakdown torque, analogous to the stalling torque of a gas engine, is roughly proportional to the motor size and the square of its magnetic flux density. The temperature limitation is exactly analogous to the temperature limit of a transformer, the useful life of the insulation being reduced exponentially as the temperature is raised.

The increased variety of motor uses in recent years, especially for automatic operation of refrigerator compressors, air conditioning, pumps, and other mechanical devices, has led to more exact methods of application, utilizing motor overload capacity and matching torque characteristics to the driven equipment. Under this economic pressure, motor overload capacities have been increased over the requirements of present standards, and small motors are now commonly used on intermittent overloads far beyond their continuous ratings.

It is proposed, therefore, that American standards be revised to provide for increased values of breakdown torque and service factor in the smaller motor ratings, and that permissible intermittent duty cycles be defined, enabling the full economic life of the motor to be utilized. It is also proposed that the starting current be recognized as a convenient and accurate measure of induction-motor breakdown torque, or momentary torque capacity, and that starting current values be established on a logical and consistent basis for both single-phase and polyphase motors.

Specific recommendations are given in the paper for rated characteristics and operating limits under this starting current-temperature system of rating, and the economic advantages to the industry to be gained by their adoption are pointed out, including safer wiring and control systems and a reduced variety of special motors.

THE importance of an adequate system of rating for industrial motors can hardly be overemphasized. The rated horsepower of a motor is a measure of its working ability, for whose integrity the entire electrical industry is responsible. The name-plate rating implies a host of different qualities built into the motor, including overload and starting ability, temperature endurance, high potential strength, and other matters covered by national standards. For the economic use of motors, the fair comparison of competitive designs, the maintenance of a proper and not excessive variety of types, the intelligent handling of power supply and control problems, and for many other reasons, it is essential that American standards of rating convey a definite guarantee of balanced characteristics and quality in motor design.

The essence of the rating problem is to find a simple test procedure that will uniquely define the output limitations of the apparatus in question. The outputs of gas engines, steam locomotives, pumps, turbines, and other mechanical apparatus are limited by mechanical considerations. Their continuous output ratings are, therefore, very little below their maximum momentary capacities, and users do not expect to load them appreciably beyond their ratings, even momentarily. On the other hand, the output of a transformer is limited almost entirely by thermal considerations, the theoretical point of maximum output with a constant voltage supply being far beyond the safe thermal limit. Hence, transformer users may permit high short-time overloads, so long as prescribed temperature limits are not exceeded.

Electric motors are subject to mechanical as well as thermal output limitations, both of which must be recognized in a practical rating system. The thermal limits are controlling in continuous operation, with present insulating materials, so that the close similarity between motor and transformer methods of rating that has always existed is entirely logical. In many cases, however, such as hermetic refrigerator motors, responsibility for cooling is entirely in the user's hands, so that usual temperature-rise guarantees will not be made by the motor manufacturer. Future trends will, therefore, assuredly require a rating system based on torque ability alone. The object of the present paper is to propose a transitional rating system, in which the temperature limits will be supplemented by other size-defining requirements.

Paper number 39-2, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 20, 1938; made available for preprinting November 23, 1938.

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History

A thorough discussion of the motor rating question before the Institute and by the entire industry some 12 to 18 years ago resulted in the adoption of the present American system of a single continuous rating with 40 degrees centigrade rise for general-purpose motors. The conclusions at that period were ably summed up by C. L. Collens in a paper published in the August 14, 1926, issue of *Electrical World*, from which the following statements are quoted:

1. Any basis of rating is at best merely an arbitrary designation of size. It is merely one of many that might be chosen.
2. Rating alone is insufficient and must always be supplemented by a clear definition of the service conditions for which the rating is chosen. In fact, determination of the usual service conditions must necessarily precede the determination of a suitable basis of rating.
3. Rating alone is an insufficient indication of the inherent ability of the motor to perform satisfactorily under service conditions and duty cycles differing from the usual. It is merely one indication of size and must be supplemented by other service information to permit of intelligent selection and economic application.

In concluding his paper, Mr. Collens made the following recommendations:

1. The division of industrial power motors into two classes with the dividing line at 200 horsepower, and in each class:
2. A normal continuous-duty single rating for the open-type motor as the standard designation of size.
3. Well-defined usual service conditions for the normal rating.
4. Service information showing permissible loadings under other duty cycles or other service conditions different from the usual service conditions.
5. Specialized motors with special ratings only where the performance characteristics required or the nature of the duty cycle do not permit of applying a service factor to the normal rating of the standard motor.

Four of these five recommendations were carried into effect in the AIEE and National Electrical Manufacturers Association standards more than ten years ago, and experience since has well justified this action. Mr. Collens' fourth suggestion, however, that information should be prepared, showing permissible loading of standard motors under other duty cycles or unusual service conditions, has never

been adequately carried out nor incorporated in the industry standards.

The progress of the art during the past ten years, including the development of the modern automatically controlled cyclic loads of air conditioning and refrigeration, has brought a tremendous increase in number and variety of motor applications. The recent trend has been to develop many special motors, each adapted to drive a particular piece of mechanical equipment, often with overload and starting abilities much in excess of those normally associated with their name-plate continuous ratings.

The objectives of revised standards should be to specify a standard type of motor adapted for the greatest variety of applications and to facilitate the economic use of the full capacity of the motor under all service conditions.

The Present Rating System

Present American standards[1] provide for two broad classes of continuous-rated motors.

General-purpose motors (200 horsepower or less and 450 rpm or more) have a single continuous rating, but must be suitable for carrying 115 per cent of rated load continuously under usual service conditions, with the ambient temperature 40 degrees centigrade or lower. These motors are offered in standard ratings for use without restriction to a particular application. They are required to meet the low limiting temperature rise of 40 degrees centigrade by thermometer at rated load, to allow a greater factor of safety where the service conditions are unknown.

Special-purpose motors, specifically designed for a particular power application where the load requirements and duty cycles are definitely known, have a single continuous rating of 50 degrees centigrade rise by thermometer, without any continuous overload requirements.

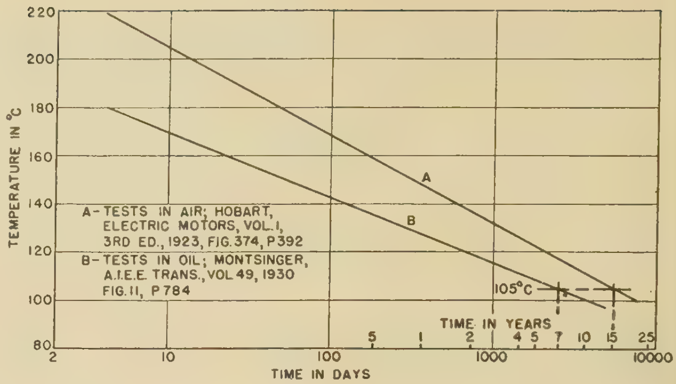
The standards also specify minimum values of starting, pull-up, and breakdown torques for each type and class of motor.

These provisions should undoubtedly be retained, but it appears desirable to add to or modify them in four respects:

First, the standards should include operating recommendations for general-purpose motors in intermittent or varying load service and in different ambient temperatures, so that the inherent overload capacity of the motor can be safely utilized. This is in accordance with the

1. For all numbered references, see list at end of paper.

Figure 1. Life expectancy versus temperature for class A insulating materials in continuous service



action already taken in the recently proposed American standards for transformers.

Second, the general use of small motors in intermittent rather than continuous service should be recognized in the standards by requiring relatively greater starting and breakdown torques, and greater temporary overload capacities, than for larger motors. Unless such provisions are made, off-standard motors will be used to an increasing extent, and control problems will be complicated, to the detriment of the public as a whole.

Third, the more general use of various protected motor designs suggests that their allowed temperature rises be reviewed and their ratings be made more nearly comparable with general-purpose motors.

Fourth, the more complete utilization of motor overload capacities, implied by this program, should be accompanied by more exact determination of insulation temperatures. In many modern designs, especially of protected* motors, the windings are quite inaccessible, and thermometer readings on exposed parts do not accurately measure hot-spot temperatures. It appears desirable, therefore, for the standards to require that stator-winding temperatures be measured by resistance. This fourth question is the subject of a companion paper.[2]

Overload Capacity of Standard General-Purpose Motors

Assuming adequate mechanical strength, the measure of a motor's momentary overload capacity is the adequacy of its torques, giving assurance that the motor can bring the load to speed and carry it under low voltage, high friction, or other unforeseen temporary conditions. American standards now require that general-purpose polyphase induction motors shall have a breakdown torque of not less than 200 per cent. Allowing for ten per cent reduction in voltage and 20 per cent margin for variations in individual conditions of loading, this 200 per cent breakdown torque will enable loads not over 135 per cent of the rating to be carried successfully, subject to heating limitations.

* Protected is used here to describe partially or fully enclosed motors as a class.

Therefore, under the present standards, motors cannot be relied upon to carry momentary overloads of more than 35 per cent in excess of the rating, under a reasonable variety of service conditions. In practice, designers normally provide more breakdown torque than required by the standards, especially for the smaller and higher-speed motors, so that many present designs can carry considerably greater short-time overloads.

It should be remembered that the starting current of a large polyphase induction motor is almost directly proportional to the maximum or breakdown torque. The starting current is, therefore, an excellent measure of short-time overload ability.

With adequate torque margins, the remaining important factor in overload capacity is the temperature rise. This must be low enough to ensure adequate service life under the expected overloads. While there may be other objections to high temperatures in special cases, the chief purpose of limiting the standard temperature rise is to protect the public from the inconvenience and loss that would be occasioned by motors with a short insulation life. As the actual life that a motor will have under a given temperature cannot be determined by acceptance tests alone, it is peculiarly important that the standards provide for safety in this respect.

When and if insulating materials of greater temperature endurance come into use, they may be utilized to permit reduction in motor size, with a higher continuous rise. It seems desirable, however, to use such higher temperature materials and limits first on totally enclosed machines, permitting interchangeable dimensions with open-type motors of present temperature limits. For open-type

motors with liberal overload torques, and reasonable efficiencies, it costs very little to provide ventilation adequate to meet present temperature limits. The only apparent gain from higher temperature limits on open-type general-purpose motors, therefore, is a small saving in the cost and losses expended in the ventilating system. For enclosed machines, however, a definite size reduction with increased temperature can usually be made without impairing operating characteristics, and this benefit may justify developing new higher-temperature insulation systems.

It is evident that the increase of copper resistance with temperature causes a decrease in efficiency with every increase in the operating temperature. In a typical case of a motor with 90 per cent full-load efficiency at 75 degrees centigrade, the efficiency will be reduced 0.5 per cent by an increase to 105 degrees centigrade copper temperature, or by 1.25 per cent if the temperature is raised to 150 degrees centigrade. Such an increase in the losses further raises the temperature, causing cumulative heating, or "temperature creep," as indicated in figure 4. This forms an effective limitation on high normal temperature rise, or continuous overloads. Rapid oxidation of oil and consequent need for separately cooled bearings and frequent oil renewal at temperatures above 100 degrees centigrade are additional reasons for limiting temperatures in continuous service.

Temperature-Life Characteristics of Insulation

In this paper, we shall assume that the allowable temperature of the motor should be so specified that a motor in continuous service at rated load and maximum ambient temperature will have adequate life, with present insulating materials. Available information on temperature life of cellulose insulation was summarized by V. M. Montsinger[3] some years ago, in connection with studies of transformer insulation under oil, that have since formed the basis for the recently published American standards for transformers.

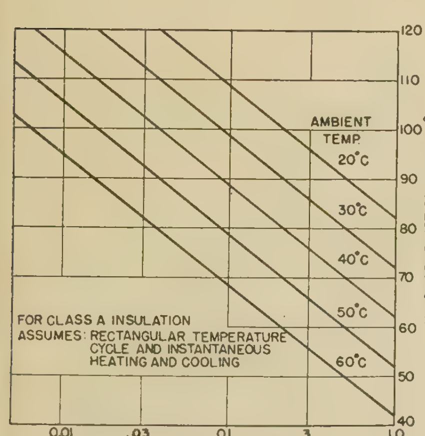


Figure 2. Permissible increase in hot-spot temperature rise to secure same life expectancy in intermittent service as in continuous service at 102 degrees centigrade

Although these test data indicate a materially longer life of cellulose materials in air than under oil, some information from longer test periods[4] that is available suggests that over long periods of time the life in air and oil will not be materially different. For the present paper, therefore, the same temperature life curve as generally used for transformers will be employed, as shown in figure 1*B*. This indicates that the life of class *A* insulation will be halved for each 8 degrees centigrade increase of temperature. The curve gives a materially shorter life than the *A* curve, which represents the best information available in 1925, when the present AIEE standards of temperature limits were established. If steadily maintained at a temperature of 105 degrees centigrade, the curves indicate the insulation will theoretically reach the end of its useful life in seven years, or will then be subject to immediate failure under any mechanical or electrical shock.

General-purpose motors with a 40 degrees centigrade ambient and a 40 degrees centigrade rise at full load by thermometer will normally have an actual hot-spot temperature of not over 102 degrees centigrade, when operated continuously at 115 per cent of the rating, in accordance with the standard service factor; indicating about ten years' useful life. Since, in practice, the average ambient temperature in the United States is generally below 30 degrees centigrade, the typical motor operating continuously at 115 per cent of its rating will have an average hot-spot temperature of about 90 degrees centigrade, giving an indicated useful life of roughly 25 years. When motors are operated below their ratings, considerably longer insulation life may be expected. This accords well with experience and indicates that our present standard basis of rating is satisfactory, for fully continuous service in normal ambients.

If, however, a motor is employed on intermittent service, with short-time overload periods repeated at intervals of hours, days, or longer, and periods of complete idleness between, the actual life of the motor at the same loading may be considerably longer, temperature alone considered. Therefore, reasonably higher temperatures may be permitted on intermittent service.

We shall assume that whether a motor is operated at a given high temperature one month in every ten, or on any other cycle with the motor idle nine-tenths of the time, the insulation will deteriorate at the same average rate, giving the same total years of life. We shall assume also that the temperature life curve is a constant exponential curve as indicated by figure 1, and that temperature variations are not accompanied by other deteriorating conditions, such as variable dirt or moisture exposure. In practice, these conditions are not strictly true, but over the moderate range of temperature variation considered, the assumptions appear justified.

Consideration of figure 1 readily enables the actual temperature to be determined that will give the same life in years on any intermittent service. Figure 2 shows, for example, that, if the motor is idle nine-tenths of the time, it may be permitted to have a hot-spot temperature rise 26.5 degrees centigrade greater than normal, and still have the same life in years as the standard motor operated continuously at 115 per cent of rating. To find the permissible overload on the motor, corresponding to this additional temperature rise, reference must be made to the characteristics of a typical general-purpose motor.

Normal Induction-Motor Characteristics

If a motor is designed solely for continuous operation at rated load, it will normally have its maximum efficiency point near 75 per cent load, giving the most favorable over-all performance. If the maximum efficiency point always occurs at the same fraction of full load, the no-load losses will bear about the same proportion to the full-load losses for all motors in the line. Keeping the same percentage of breakdown torque for all sizes of motor, in accordance with the present standards, and keeping the same balance of losses as indicated, the performance curves and equivalent circuit of the typical general-purpose induction motor can immediately be determined. The chief differences between motors of different speeds and horsepower will be that the percentages of no-load current, full-load power factor, and total losses will vary. Figure 3 shows the performance curves and the equivalent circuit for such a typical polyphase motor, which closely represents an average 4-pole 25-

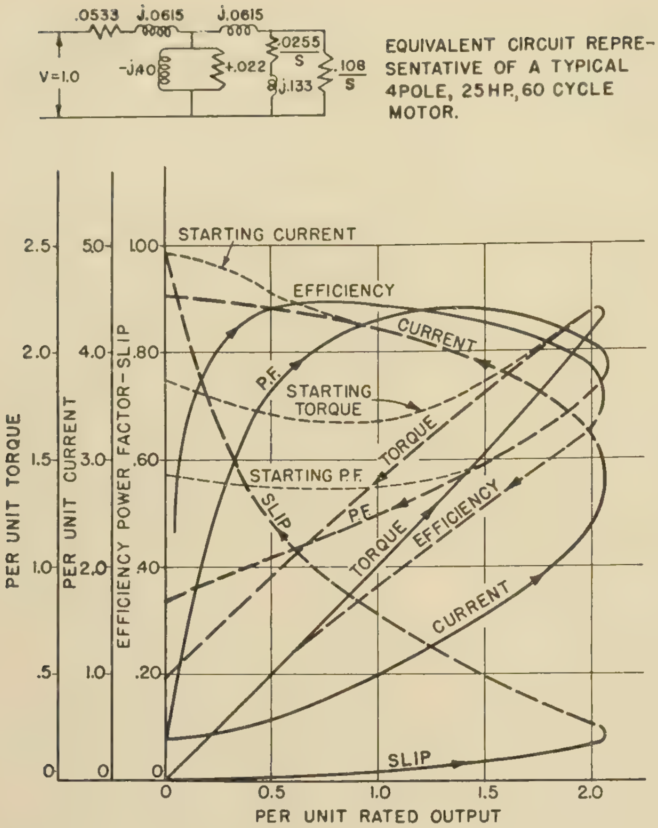


Figure 3. Operating characteristics of a typical four-pole general-purpose polyphase motor

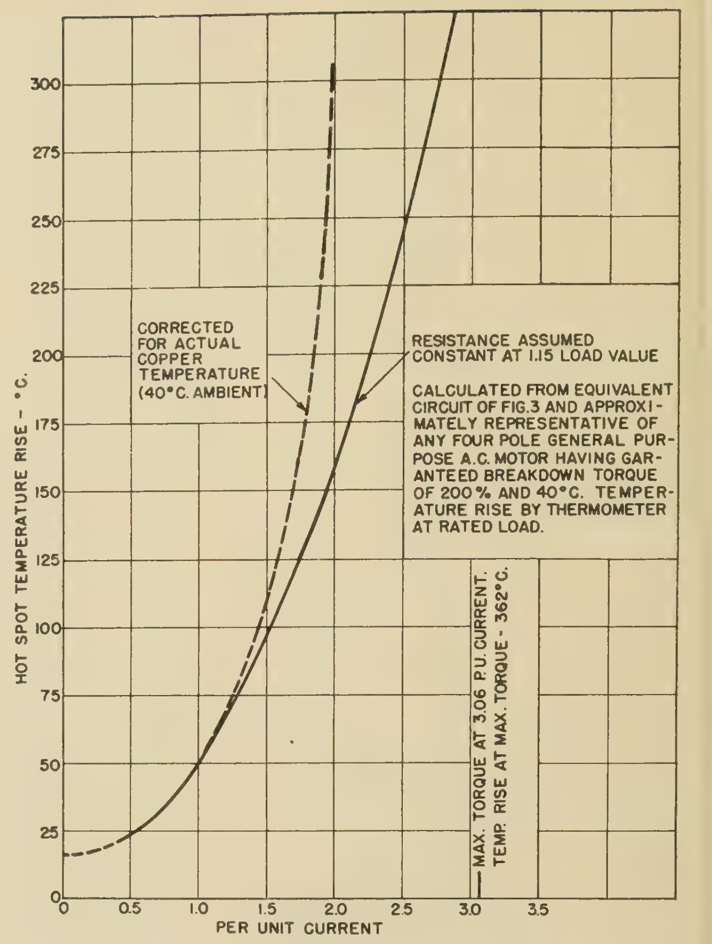


Figure 4. Hot-spot temperature rise versus current for typical four-pole general-purpose motor

horsepower 220-volt design. The circuit was chosen to give a maximum torque at normal voltage of 220 per cent (10 per cent greater than the 200 per cent value required by the standards), providing the usual margin necessary to be sure of meeting guarantees.

Many polyphase induction motors now have double squirrel-cage or deep-bar rotors, to get increased starting torque without impairing efficiency in normal operation, and the circuit constants shown in figure 3 are representative of this type. Light and heavy broken-line curves indicate the starting characteristics of the double and single squirrel-cage designs, respectively. The curves indicate that a starting (locked rotor) current of at least 500 per cent of full load must be allowed, if 200 per cent breakdown torque is required. The actual value of starting current will vary for different sizes and speeds of motor, depending on their efficiency, power factor, and starting torque values, but a figure of 14 amperes per rated horsepower for 220-volt 3-phase 60-cycle motors larger than 15 horsepower represents the lowest that can be expected. Any increase in breakdown torque will require a proportional increase in starting current, 250 per cent breakdown torque requiring 17.5 and 300 per cent requiring 21 amperes per rated horsepower on the same basis. The smaller motors also will have greater starting currents for a given breakdown torque than large motors, due to their lower power factors and torque efficiencies, and higher starting-torque requirements.

Many temperature tests on different motors indicate that for low-voltage designs the temperature rise of the winding is closely proportional to the total losses in the motor, regardless of where they occur.[2] For overload conditions especially, the hot-spot temperature rise may be taken as directly proportional to the total losses without important error.

If, therefore, we assume that all motors are provided with ventilation just adequate to hold the temperature down to the rated value of 40 degrees centigrade rise by thermometer at full load, a single temperature - rise - versus - current curve may be drawn, that will be closely representative of a wide range of motor sizes and speeds. Such a curve is shown in figure 4, the temperature rise being in direct proportion to the losses given by the equivalent circuit of figure 3. Degrees are plotted against percentage of full-load current, rather than against horsepower or torque, to allow for the variations in the no-load current and no-load temperature rise for motors of different sizes and speeds. The dotted curve indicates the additional temperature rise due to increase of copper resistance with temperature,* showing the cumulative heating effect of prolonged overloads.

Although figure 4 is drawn for a motor with exactly 220 per cent breakdown torque, it may be applied to other breakdown torque values by a proportionate change in scale. The curve shown gives hot-spot temperatures, rather than simply observable values. Many heat runs have shown that the actual hot-spot temperature in a low-voltage general-purpose induction motor at rated load is generally about five degrees centigrade above the highest thermometer rise, and rarely exceeds it by more than ten degrees centigrade. Thus, the conservative assumption is made that a standard motor with 40 degrees centigrade rise by thermometer will have 50 degrees centigrade rise at the hottest spot on the insulation.

* Neglecting the increase in watts dissipated per degree of rise at elevated temperatures.

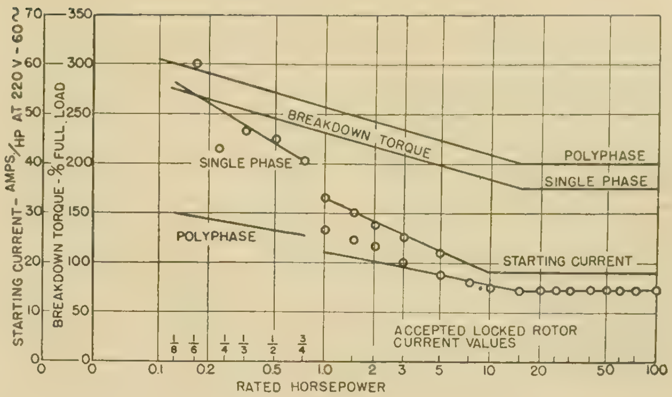
At the service factor rating of 115 per cent load, the motor losses are 123 per cent of full-load losses (figure 3), making the temperature rise 49 degrees by thermometer or 62 degrees at the hottest spot. Thus, the service factor rating corresponds very closely to the 50 degrees centigrade rise permitted by the standards for special-purpose motors, of which the load conditions are definitely known in advance. The 13 degrees centigrade rise at the hot spot above the test value of temperature rise assumed for this typical low-voltage induction motor is a little less than the 15-degree allowance in the standards.

Permissible Intermittent Overloads

Comparison of figures 2 and 4 enables us to determine the permissible overload current on a standard motor for any degree of intermittency of loading. The full-line curves of overload current versus fractional operating time for different ambient temperatures, shown in figure 5, all correspond to the same ten-year life expectancy. The lower of the two 40-degree-centigrade ambient curves indicates the effect of temperature creep, corresponding to the dotted curve of figure 4.

Inspection of this figure shows that short-time overloads well in excess of 135 per cent of rated current are permissible in usual 20 degrees centigrade to 30 degrees centigrade ambients, without exceeding economic heating limits. As previously indicated, however, this 135 per cent value is roughly the highest load the present standard motor can carry without risk of breakdown under ten per cent low voltage and other varying conditions of service. Hence, to utilize fully the economic life of motors in intermittent service, higher breakdown torques than 200 per cent are required.

Figure 6. Recommended breakdown torques and starting currents (locked rotor) for four-pole general-purpose a-c motors of different horsepower ratings



It should be noted that figure 5 is derived on the basis of no lag of temperature behind the applied load, or instantaneous heating and cooling. For duty cycles involving many hours of continuous running interspersed with long idle periods, this is satisfactory, but for running periods of two hours or less, it gives very conservative values. At the limit, with very short cycles, the average loss can be assumed constant over the entire period, and the aging will correspond to the temperature rise due to $1/n$th of the operating plus starting losses, if the idle time is $(n - 1)$ times the operating time.

Knowing the heating and cooling curves, or thermal time constants, of any motor, it is readily possible to determine the temperature-time curve, and hence the aging effect, of any duty cycle. In appendix II of this paper, such calculations have been carried out, and the curves of figure 11 have been derived, enabling the permissible temperature rise above normal to be determined, and hence, from figure 3, the permissible overload current to be found, for any actual cycle, and for motors of different sizes.

Everything considered, therefore, it seems proper to use the ideal zero time constant curves of figures 2 and 5 for operating recommendations, recognizing that the extra heating due to acceleration losses in starting, and temperature creep, on higher overloads, offset a large part of, but not all of, the beneficial effects of time lag in temperature rise on short cycles.

Characteristics of Motors Designed for Intermittent Service

If a continuous-rated motor is applied on an intermittent load, therefore, and the insulation is to be used to the full extent of its economic life, the motor can evidently be operated at an output greater than the continuous rating, and it should be designed with a higher breakdown and starting torque ability than required for continuous service. Knowing the fractional operating time and the desired motor life, the permissible temperature rise is found from figure 2. For greatest economy, the motor should then be designed to obtain the maximum possible horsepower output at this temperature rise. The designer does this by first varying the number of turns in the motor winding, and finding for each case the current loading with the given supply voltage that gives a total power loss corresponding to the permitted temperature rise. The maximum power output for these conditions is obtained when the maximum point on the efficiency characteristic coincides with the loading that gives the permitted total losses.

In practice, the maximum efficiency point should occur a little below the operating point to secure better light-load efficiency and power factor and lower starting current. We shall assume, therefore, that the winding turns will finally be chosen to make the maximum permissible load for the desired intermittency always occur at the 115 per cent load point on the standard-motor characteristic curves, giving the same torque characteristics as in continuous operation at the original 115 per cent service-factor rating.

If the equivalent circuit constants of the motor do not change from increased

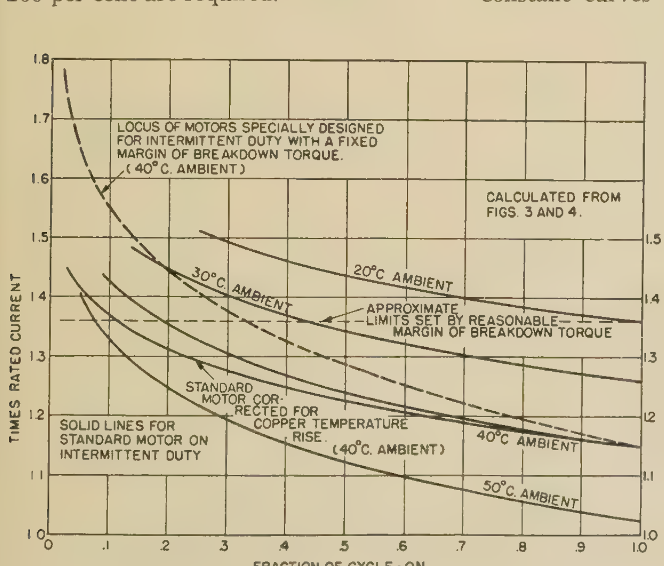


Figure 5. Permissible current loading of four-pole general-purpose motors in intermittent service, for the same insulation life expectancy

magnetic saturation or other cause, and all fixed losses increase as the square of the volts per turn,* all the characteristic curves of the motor will retain the same shapes, and figure 3 will still represent the motors redesigned for higher torques.

The per unit output of the redesigned motor is obtained by multiplying the output scale for the original motor, figure 3, by $1/a^2$, where a is the ratio of the new to the old number of winding turns, the new full load characteristics being the same as at a load a^2 on the original motor. The rated load in horsepower is assumed to remain the same in all cases.

By this process, the efficiency and, therefore, the total losses and temperature rise at rated load will be slightly changed from their original values. A limit is set on this stepping up of the torque with reduced winding turns by the excessive increase of no-load current when the magnetic flux density is increased beyond the saturation point. For low-speed motors particularly, there is a definite value of volts per turn of winding beyond which a further increase will reduce instead of increasing the breakdown torque, for fixed magnetic dimensions. A further limitation is set by the rapid increase in full-load current and temperature as the maximum efficiency point is brought beyond the point of rated load.

For example, taking 40 degrees centigrade ambient temperature and an ideal load cycle with only 0.06 of the time operating, the rest idle, the permissible motor temperature rise at the hottest spot is 94 degrees centigrade, as compared to 62 degrees centigrade for the standard motor operating at 1.15 times rated load, from figure 2. Hence, the allowable total losses can be increased to 1.52 times, and the volts per turn should be raised by a factor of 1.23. At this increased magnetic density (neglecting saturation), the motor will have an output at 94 degrees centigrade hot-spot temperature rise of 1.52 $\times$ 1.15 or 1.75 times the original horsepower rating, and the breakdown torque will be 1.52 times larger, or will still be 174 per cent of the output (190 per cent including the 10 per cent margin of the average motor of figure 3), the same as for the original motor operating at the 115 per cent service-factor rating.

Figure 7 compares the calculated temperature rise-current curves for the motor of figure 3 before and after rewinding with 1/1.23, or 0.81 as many turns, with the same supply voltage, the same ventilation, and the same continuous horsepower rating. Rated load efficiency and power factor on the new characteristic curves are the same as at $(0.81)^2$, or 0.66 load for the original winding, or 89.3 per cent and 79 per cent, respectively, from figure 3. These compare with 88.9 per cent efficiency and 86.5 per cent power factor for rated load on the original motor, giving a new rated load current 108 per cent of the original value, in amperes.

The total losses at rated load being 10.7 per cent instead of 11.1 per cent of input, the temperature rise at rated load will be 38.5 degrees centigrade by thermometer, or 48 degrees centigrade at the hot spot, a little lower than before. The efficiency at 115 per cent load on the rewound motor is 89.8 per cent as compared to 88.2 per cent originally, so the temperature at this load is $1.15 \times 10.2/10.7 \times 89.3/89.8 \times 48 = 52.5$ instead of 62 degrees centigrade. From figure 7, 62 degrees centigrade rise is reached at 122 per cent current on the new motor, which, from figure 3, corresponds to 132 per cent load. Hence, the redesigned motor has a service factor of 1.32 in place of 1.15. If, however, the 40 degrees centigrade rise at rated load had been adhered to, 62 degrees centigrade rise would be reached at 119 per cent current, corresponding to 125 per cent load.

The overload current-temperature curves of figures 5 and 7 apply fairly closely to a wide range of motor sizes, but differences in power factor and efficiency curves will cause considerable differences in the corresponding output curves for different sizes. In general, the lower full-load power factor and higher no-load losses of the smaller motors will cause their overload currents to increase less rapidly than their outputs, so giving them higher service-factor ratings than large motors.

Recommended Characteristics

Since small motors are usually applied on intermittent loads, it is obviously desirable to design them for a moderate degree of intermittency rather than for continuous service. Experience has shown that motors with minimum breakdown torque values varying from 200 per cent for 15 horsepower and larger, up to 300 per cent for 1/8 horsepower, are well suited for average starting and load requirements, and it is proposed that these values be adopted as standard for polyphase motors, in place of the flat value of 200 per cent now standard. On small motors with high starting torque, which have more than 20 per cent slip at maximum torque, the torque at 80 per cent speed is taken as the breakdown value. This 80 per cent speed point is chosen as the measure of breakdown torque in recognition of the fact that higher torques which may be available at lower speeds are only useful in starting, rather than in overload operation. Integral-horsepower normal-torque motors usually have less than 20 per cent slip at breakdown. Figure 6 shows the proposed variation of torque with horsepower rating.

In comparing single and polyphase motors for the same service, it is found that the single-phase motor (of either repulsion or capacitor type) has normally greater starting torque, greater service factor, and lower slip for the same value

* Actually, the friction and windage will not increase, unless larger bearings are required for the higher torques, but the core and exciting current losses will increase somewhat faster than the volts per turn squared. The two effects will counteract each other so completely that their combined effect may be neglected for our purposes.

Table I. Characteristics of Proposed Standard General-Purpose A-C Motors With 40 Degrees Centigrade Rise by Thermometer at Rated Load

	Breakdown Torque* in Per Cent of Full-Load Torque				Locked Rotor Current Amperes at 220 Volts, 60 Cycles				
	Single Phase		Polyphase		Single Phase		Polyphase		
Horsepower	Present	Proposed	Present	Proposed	Present	Proposed	Present	Proposed	Service Factor in 40 Degrees Centigrade Ambient
1/8	175	275	200	300	10	7		3.75	1.50
1/6	175	270	200	295	10	9		5	1.45
1/4	175	260	200	285	10.75	12.5		7	1.40
1/3	175	255	200	280	15.5	16		9	1.35
1/2	175	245	200	270	22.5	22		13	1.30
3/4	175	235	200	260	30.5	30.5		19	1.30
1	175	230	200	255	33	33	26.6	22	1.25
1 1/2	175	225	200	250	45	45	36.6	31.5	1.25
2	175	215	200	240	55	57	46.6	40	1.20
3	175	210	200	235	75	77	60	57	1.20
5	175	200	200	225	110	110	89	90	1.15
7 1/2	175	190	200	215		145	120	120	1.15
10	175	185	200	210		180	150	155	1.15
15 and up	175	175	200	200		18 $\times$ horsepower		14.5 $\times$ horsepower	1.15

* When the maximum torque occurs at more than 20 per cent slip, the torque at 80 per cent speed is taken as the breakdown value.

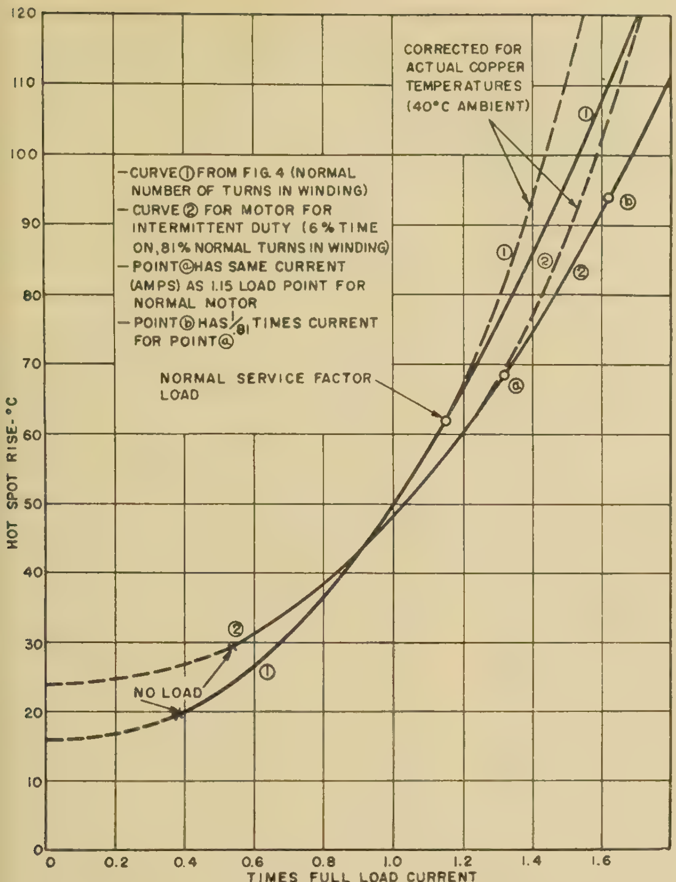


Figure 7. Hot-spot temperature rises of standard and intermittent-duty types of motors

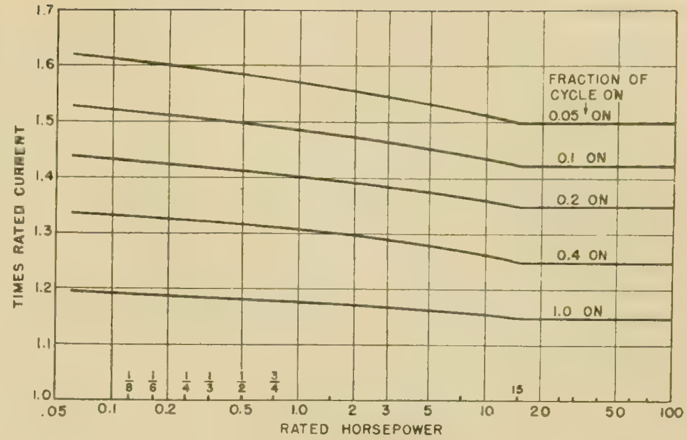


Figure 8. Permissible current loading, in intermittent service, with 40 degrees centigrade ambient, of four-pole general-purpose a-c motors, having breakdown torques in accordance with figure 6

of breakdown torque. A repulsion-start motor, for example, usually has a starting torque of over 400 per cent, and a pull-up torque considerably less than the breakdown value, while a small polyphase motor has a starting torque only a little greater than the 80 per cent speed value, and a pull-up torque fully equal to the breakdown value. Capacitor-start single-phase motors have intermediate characteristics. Experience has shown that the characteristics of the two types are best matched when the breakdown torque of the single-phase motor is equal to about 90 per cent of the polyphase value. Present standards, recognizing this, require only 175 per cent breakdown torque for single-phase motors. It is proposed, therefore, that single-phase-motor breakdown-torque values be standardized 25 points lower than for polyphase, varying from 275 per cent for $^1/_8$ horsepower to 175 per cent for 15 horsepower, figure 6.

In figure 6 also are shown proposed limiting starting-current (locked rotor) values for single and polyphase motors, chosen to enable the specified breakdown torque values to be obtained with reasonable margin. Circled points show present accepted, but inconsistent, values of starting current. The amperes per horsepower increase in the smaller sizes in approximately direct proportion to the increase of breakdown torque, and in inverse ratio to the apparent efficiency at rated load. The break in the current curves between fractional and integral horsepower ratings is due to the higher starting-torque requirements in the fractional sizes, to provide for relatively higher friction and greater variations in service conditions. The general use of repulsion motors, which inherently have a higher ratio of starting to pull-up torque, and lower starting currents, than capacitor-start motors, has made it customary to specify much higher starting torques for fractional-horsepower motors than required by the load in most cases. It may be, therefore, that future trends will reduce these starting torque requirements below the present levels of about 400 per cent of rated value, and in this case the fractional-horsepower starting-current levels could be reduced also.

The single-phase locked-rotor currents given are proposed to cover capacitor-start motors, and are appreciably higher than necessary for repulsion-start motors. The proportional relation between starting current and breakdown torque also does not hold as accurately for repulsion-start motors as for pure induction motors, due to possible changes in brush position on the repulsion types.

These starting-current values are especially important to control engineers, underwriters, and power-supply authorities, because they determine the necessary fusing, overload control, wiring, and voltage-regulation requirements. From every viewpoint of safety and convenience, it is desirable to have a definite, one-to-one correspondence between locked-rotor current and name-plate horsepower for a given supply voltage. If the values proposed in figure 6 are adhered to, they will permit economic motor use with uniform safety and control practice, and give reasonable assurance that a motor of the next lower or higher rating capacity is not masquerading under a false name plate.

The dotted line in figure 5 shows the permissible overload current capacities without reduction of breakdown-torque margin, obtained by using a different winding for each load cycle, with 81 per cent turns for 0.06 operating time, 84 per cent for 0.10 time, etc. By designing each size of the line of motors to have the breakdown-torque value indicated in figure 6, and using these modified motors for all duty cycles, reasonably increased overload capacity can be obtained with normal efficiency, at the expense of reduced full-load power factor and increased starting current, but with starting torque higher in proportion to the increased breakdown torque.

Figure 8 shows the permissible current overloads in intermittent service, for different sizes of motor, each designed to meet the breakdown torque requirements of figure 6, and to have 40 degrees centigrade rise by thermometer at rated load. Figure 9 shows typical current-output curves for these same motors. From these two sets of curves, figure 10 is derived, giving the permissible torque overloads, or power outputs, corresponding to the

current overloads of figure 8. It is interesting to note that the service factor varies from 1.15 for 15 horsepower and up, to 1.25 for 1 horsepower and 1.50 for the $^1/_8$-horsepower ratings. The values given have been rounded off to give a single conservative figure for both polyphase and single-phase motors.

It is important to keep in mind the reasons for the high service factors on fractional-horsepower motors. The service factor represents the output increase obtained by allowing the temperature rise to increase 10 degrees above the rated value of 40 degrees centigrade. This gives 1.15 to 1.19 times rated current over the range of $^1/_8$ to 100 horsepower 4- and 6-pole motors, figure 8. Fractional-horsepower motors, having inherently lower power factor and efficiency, and being called on to deliver higher starting and breakdown torques than large motors, inevitably have no-load current values only slightly lower than at full load. Hence, their current-output curves, figure 9, are much flatter than for large motors. Small motors have also inherently large mechanical factors of safety, as their shaft and bearing sizes are fixed by stiffness rather than torque requirements. Finally their large surface areas per unit of power loss, and their thin insulation give them relatively much better heat dissipating characteristics than large motors. Electrical, mechanical, and thermal characteristics all combine, therefore, to make lower service factors appropriate for large than for small motors.

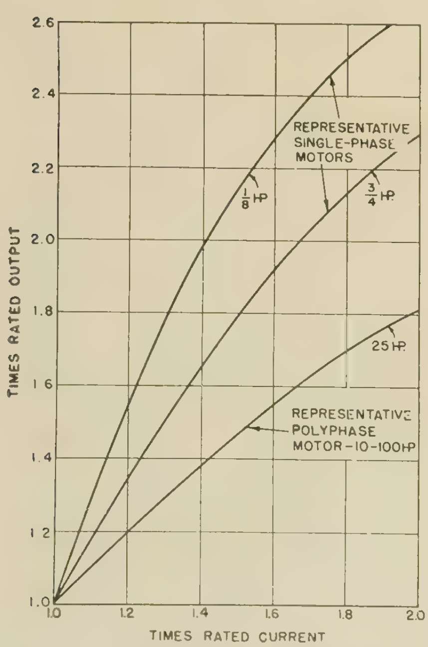


Figure 9. Typical current versus horsepower curves for four-pole general-purpose motors, having breakdown torques in accordance with figure 6

Assuming that the breakdown torques of figure 6 are adopted as standard, and keeping a continuous single rating of 40 degrees centigrade rise by thermometer at full load, the resulting line of motors will have the following characteristics, as listed in table I:

1. All motors will have 40 degrees centigrade rise by thermometer, 50 degrees centigrade or less hot-spot rise at rated load, and 62 degrees centigrade or less hot-spot temperature rise at the service-factor load.

2. All motors above 15 horsepower will have a service factor of 1.15, and smaller motors will have higher service factors up to 1.50 for $^1/_8$ horsepower, as shown in figure 10.

Thus, continuous operation could be permitted at 115 per cent to 150 per cent of rated load for the various motor sizes, in a 40 degrees centigrade ambient, with an expected insulation life of about ten years. Lower ambients usually experienced, and noncontinuous operation, will lengthen the normal insulation life at rated load to 25 years or more under usual service conditions.

3. The breakdown torque and locked-rotor current values for different horsepowers will be as shown in figure 6. Starting torque values will be proportional to breakdown torques, but relatively higher in the fractional-horsepower ratings.

4. The permissible overloads, or intermittent service factors, in percentages of rated current and rated torque are shown in figures 8 and 10 for several ratios of operating to elapsed time. Limits set by a breakdown torque margin of 150 per cent are indicated.

These values are based on the conservative assumption of zero time lag of temperature. More exact values, taking into account the actual heating and cooling time constants, can be determined by the methods outlined in appendix II.

Rating of Protected Motors

The general use of protected motors with different degrees of enclosure suggests that their permissible overloads should also be determined. Present standards allow 50 degrees centigrade rise by thermometer, for splashproof motors, and 55 degrees centigrade for totally enclosed and fan-cooled designs, with a service factor of 1 instead of 1.15. The extra 5 degrees centigrade for enclosed motors is generally understood to be allowed because of the smaller difference between the hot test spot and the measured temperature than in open motors. The standards imply, therefore, that all fully protected motors will have a hot-spot temperature rise at rated load of 65 degrees centigrade, or practically the same as that of the standard general-purpose motors of the open type at 115 per cent load.

Hence, all the curves already derived for general-purpose motors apply equally well to enclosed motors if the actual loads are divided by 1.15, or if the rating of the enclosed motor is taken as 87 per cent of the name-plate value.

It is very desirable to build protected and open-type motors in the same frame size and with interchangeable characteristics, and it may be urged that the exclusion of dirt, excessive moisture, and other protection to insulation in enclosed motors justifies a higher temperature for the same service. It appears probable that new insulating materials may permit this in future, but further operating experience records should be obtained before the standards are changed in this respect.

In usual enclosed motor designs, fewer winding turns and larger magnetic dimensions are employed than in open motors, to reduce the copper losses and temperatures. Hence, such motors normally have a little higher breakdown torque and starting current than open motors, but still within the limits of figure 6, and they are even better adapted to carry short-time overloads. For this reason, and in view of the presumably longer insulation life at a given temperature because of moisture and dust exclusion, it is suggested that service factors be applied to enclosed as well as open motors. While no specific recommendations are now offered, it is clear that the inherent ability of an enclosed motor to deliver short-time overloads will be utilized in the long run, and standards or operating recommendations should be prepared to facilitate this.

The cooling of motors for hermetically sealed refrigerators, enclosed gas pumps, and other built-in applications, is entirely in the control of the user. Temperature rating standards do not apply to such motors, therefore, and the operating temperature may be anything that the user's experience justifies. By adhering to the proposed breakdown-torque and starting-current rules, and normal efficiency values, however, assurance is given that the motor will have the torque ability represented by the name-plate horsepower. With improvements in insulating materials, such torque and starting-current rating methods may be expected to supersede the temperature system to an increasing extent.

Conclusions

The whole object of this discussion is to make the standard motor fit as many uses as possible. It should have a simple,

generally understood name plate that guarantees its satisfactory performance in continuous duty, and there should be supplementary information describing its proper application in all sorts of different duties.

The recommendations for breakdown torque and starting current values for standard general-purpose single-phase and polyphase induction motors given in table I, and the associated permissible overloads in intermittent service, figure 10, are believed to provide a logical and comprehensive system of rating. The information on permissible overloads is proposed in the form of operating recommendations, rather than as hard and fast requirements of standards. This system provides for motors designed to fit the maximum variety of applications, and it permits the user to apply them intelligently up to the limits of their economic life. It ties together the present differing practices on fractional- and integral-horsepower motors and permits merging the important requirements for automatically controlled refrigeration and air-conditioning motors with the standard general-purpose designs.

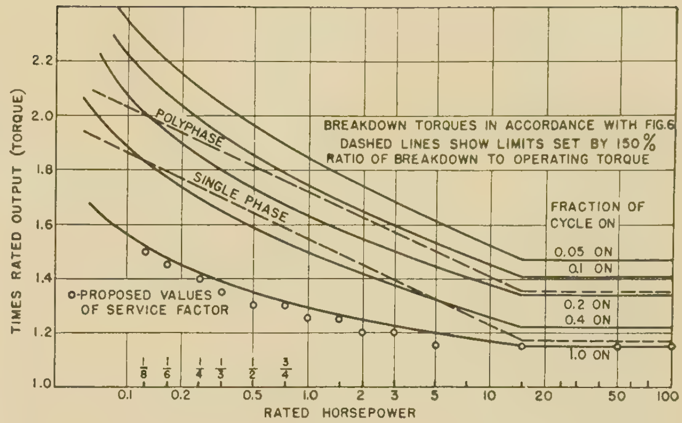
The plan does not disturb the basic principles of motor rating so well established some 15 years ago, but carries them forward along proved lines. It divorces the operating recommendations from the rating standards, and lays a foundation for the more flexible use of motors in new fields and their more ready adaptation to future developments.

The numerical values given in the paper have been presented as illustrative of the principles involved, rather than as final values for standards. Necessarily, the characteristics of motors made by different manufacturers will vary from those used for illustrations in the paper, and an industry-wide review of experience must be made before any action on standards can be taken. It is also desirable to extend the analysis to cover motors of other speeds besides the four-pole designs which have alone been considered in the paper. And, finally, the temperature-life curve of figure 1, forming the basis of the values proposed, should be reviewed on the basis of new information available, before final values for standards are accepted.

Appendix I. European Standards of Motor Rating

Comparison of French,[6] British,[7] and German[8] with American standards reveals considerable differences in both torque and

Figure 10. Permissible overloads of typical four-pole general-purpose a-c motors in intermittent service, in 40 degrees centigrade ambient



temperature limits for general-purpose a-c motors. The quantity of work-performing ability measured by one continuous horsepower of motor rating cannot be accepted as an international standard, therefore, but must be evaluated by reference to the customs in the country of origin.

All four countries give general-purpose industrial motors a single continuous rating, limited by temperature rise. The first three countries allow a maximum ambient temperature of 40 degrees centigrade, but the Germans allow 35 degrees centigrade ambient. American and British rules for open-type motors specify 40 degrees centigrade rise by thermometer; while French rules specify 55 degrees centigrade by resistance, or 50 degrees centigrade by thermometer if resistance measurements are not practicable, and German rules specify 60 degrees centigrade by resistance or thermometer, whichever gives the higher reading. In all cases, temperatures are measured before and after shutdown, the highest reading being taken and no tolerances from the guaranteed values being allowed.

For these open-type low-voltage motors, it is fair to say that the hot-spot temperature rise is not more than 25 per cent above the thermometer reading, or 12 per cent above resistance measurements. On this basis, the hot-spot temperature rises of the four motors at rated load will be 50 degrees, 50 degrees, 62 degrees, and 67 degrees centigrade, respectively. In continuous operation at full load in a 40-degree-centigrade ambient, therefore, the American and British motors will have a life expectancy of 25 years, the French motor 10 years, and the German motor 6 years, from figure 1, assuming the same design margins over guarantees. American special-purpose motors, or general-purpose motors operated at their 115 per cent service-factor rating, however, have the same temperature rise, and the ten-year life expectancy, of the French motor. In a 35-degree-centigrade ambient, these periods would all be increased about 50 per cent.

For totally enclosed motors, the American rules allow 55 degrees centigrade rise by thermometer without any service factor, and the British allow 50 degrees centigrade, while the French and Germans keep the same 55- and 60-degree rises by resistance allowed for open motors. In a 40-degree-centigrade ambient with continuous operation at rated load, these correspond to life expectancies of five, ten, ten, and six years, respectively, the American having the shortest life in this case.

The five-degree spread between open and closed motors under the American rules may be justified technically on the grounds of a lower hot-spot differential, slower deterioration of insulation from other causes than temperature in enclosed motors, and the frequent use of these motors in outdoor installations at lower ambients. It is economically sound, because it is relatively a great deal more expensive to lower the temperature of a fully enclosed motor than of an open motor, and because it facilitates interchangeable mounting dimensions for open and closed motors.

The inherent, or short time, capacity of a motor is determined by its maximum torque, which is quite independent of the temperature rise. For a true comparison of an induction motor's ability to handle all sorts of loads, therefore, it is necessary to know its breakdown torque.

The American and British rules both specify a minimum of 200 per cent breakdown torque, or 100 per cent overload torque, for standard general-purpose industrial motors. The French rules require only 150 per cent, and the German rules 160 per cent breakdown torque, on the basis of continuous ratings. The French rules recognize seven, the British two, and the German four kinds of intermittent or short-time ratings, for which 200 per cent breakdown torque is required by all except the French rules. None of the rules prescribe any starting-current limits for industrial motors.

The breakdown-torque values, which are the best measure of magnetic dimensions and mechanical ability, indicate that the standard American, British, French, and German continuous-rated motors have relative sizes of 100, 100, 75, and 80, respectively.

The American, British, and French all have special rules for fractional-horsepower motors, which cover motors smaller than one horsepower at 1,500 rpm, one horsepower at 1,000 rpm, and 600 watts, respectively. American rules prescribe the same breakdown torque and temperature limits as for larger motors, except that 175 per cent breakdown torque is required for single-phase motors. British rules require only 125 per cent breakdown torque for polyphase and 100 per cent for single-phase motors. They also extend the 50-degree temperature-rise limit to include drip-proof as well as totally enclosed motors, keeping 40 degrees centigrade for open motors. French rules provide a special "domestic" service rating for fractional motors, which is defined as equivalent to continuous service

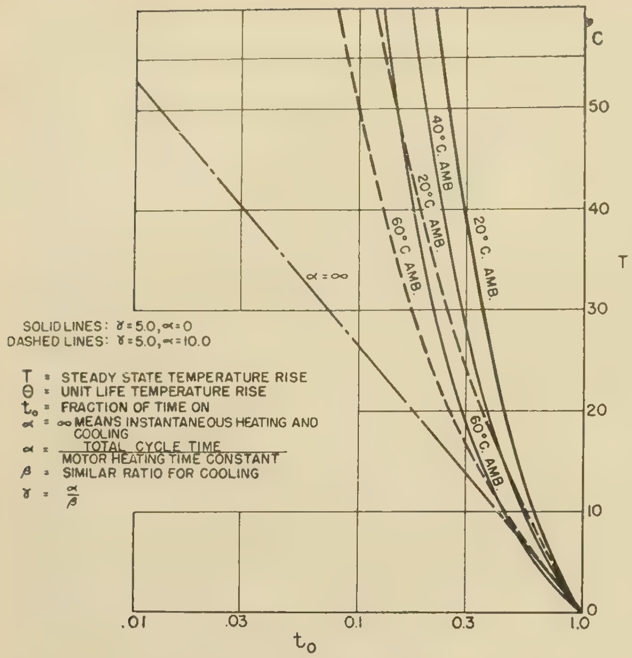


Figure 11. Permissible excess temperature rise in intermittent service, for fixed total insulation life (showing effect of cycle time compared to heating time constant)

at two-thirds of the name-plate horsepower, implying a breakdown torque 225 per cent of the continuous capacity. The French also provide a higher temperature rise for all fractional-horsepower motors, 65 degrees centigrade for continuous-rated motors, and 85 degrees centigrade for domestic motors for temperate climates, the latter on the basis of a maximum ambient temperature of only 20 degrees centigrade.

It is thus evident that continental standards call for materially smaller motors for a given continuous rating than American and British standards. R. Langlois-Berthelot has recently written a comprehensive article[9] on the temperature life of electrical machines, including an historical summary of the subject. His views on the normal life of insulation are summarized in the following statements:

> We shall take . . . the (hot spot) temperature θ (for rating purposes) as that permissible in a machine to assure in practice a normal life of 15 to 20 years, and we shall assume as a fact of experience that this normal life is equivalent to continuous operation for two years, or 17,000 hours, at the maximum temperature; so that . . . θ will correspond on the insulation-life curve to a life of two years. These are the average normal conditions, which take account of the usual variations of ambient temperature and of load. This interpretation of the temperature θ conforms to experience with machines and to the opinion of known experts who have been willing to express their views.

This point of view that the standards ought to set temperature limits to give long life under average conditions of reduced load and reduced ambient appears fundamental in continental standards, in strong contrast to the American viewpoint that the rated temperature should be low enough to assure a long life in rated load operation at the maximum ambient temperature.

Appendix II. Determination of Permissible Insulation Temperatures

In accordance with the experimental evidence that insulation deteriorates at a rate that is greater the higher the temperature, it is evident that, for the same useful life, a motor which is to be in operation only a part of the total time may be allowed to run hotter than a motor which is to be used continuously. In the body of the paper permissible overload durations were established, to secure the same total insulation life as at continuous full load, under the assumption of instantaneous heating and cooling. In practice, the motor takes an appreciable time to heat up, thus lowering the temperature in the initial part of the overload period, so that the actual temperature aging for the duty cycle is less than that assumed. In this appendix, the effect of this time lag of the temperature on the permissible duration of overloads will be considered.

It is assumed that the mode of variation of class A insulation life with temperature is that shown in figure $1B$, which represents generally accepted data. According to this curve, the effective insulation life is given by:

$$L = L_0\epsilon^{-a(T-\theta)} \tag{1}$$

where

L_0 = years of useful life at some reference temperature
= seven years at 105 degrees centigrade continuously

$T - \theta$ = temperature of insulation above reference temperature

a = a constant, here equal to 0.0866 (degrees centigrade)$^{-1}$

Then the *rate of deterioration* of insulation must be of a reciprocal form.

$$R = A\epsilon^{+a(T-\theta)} \text{ (where } A \text{ is a constant)} \tag{2}$$

For the economic use of the temperature-life characteristic of the insulation, it is necessary to choose a temperature which, if maintained continuously, will allow a reasonable insulation life, henceforth to be called *unit life*, and then so to order the temperature duty cycles as to arrive at this unit life, no more, no less. This unit-life temperature is taken as 102 degrees centigrade (giving a unit life of about nine years), since this is closely the temperature attained by a standard general-purpose polyphase open induction motor having 40 degrees centigrade rise by thermometer at full load (50 degrees centigrade rise at hot spot), when running at the 115 per cent service factor load in a 40-degree-centigrade ambient temperature. The difference between this unit-life temperature (102 degrees centigrade) and the ambient temperature is called the *unit life temperature rise* and denoted by θ.

For the purposes of the present discussion the only type of load duty cycle which will be considered is the on-off type, such as experienced by a motor driving a refrigeration or air conditioning load. This assumes a load essentially constant to be applied for some fraction of the total cycle time, following which the motor is shut down for the remainder of the cycle.

To simplify the analysis only two types of temperature variation with load will be discussed: first, where the temperature instantly attains its steady-state value corresponding to the load applied, and, second, where the temperature varies exponentially, growing and decaying according to time constants which are characteristic of the particular motor.

Consider the case in which the temperature of the insulation exactly follows the steady-state value corresponding to the load applied. The temperature duty cycle has the same appearance as the load duty cycle: a simple rectangle having a height T (temperature rise above ambient) during the fraction of the cycle that the motor is on, and zero height (ambient temperature), while the motor is shut down. Then, for this temperature cycle to give unit life it is necessary that the total deterioration of the insulation during on and off periods be the same as if the insulation had remained at the unit-life temperature rise θ. Then the following equation must be satisfied:

$$At_0\epsilon^{+a(T-\theta)} + At_f\epsilon^{+a(-\theta)} = A \times 1.0 \tag{3}$$

where

t_0 = fraction of cycle time motor is on (constant load)

t_f = fraction of cycle time motor is off

$t_0 + t_f = 1.0$

Equation 3 has the effect of averaging the actual temperature variation into the steady temperature θ. With the usual values of θ this simplifies to:

$$t_0\epsilon^{+aT} = \epsilon^{+a\theta} \tag{4}$$

which gives the *straight* lines shown on figures 2, 11, and 12, when plotting T or $(T-\theta)$ against log t_0. As indicated in the body of this paper, it is a simple matter to translate such curves into the equivalent load or current curves if the steady-state temperature versus load or current characteristics of the particular motor are known.

As it has been assumed that the rate of deterioration of insulation depends only upon temperature, the results calculated on the basis of the above equation are correct for cycles of any time length, whether one hour, or several years. Thus, it is evident that standard motors possess considerable inherent overload ability (may ex-

ceed their normal temperature rise), judging from thermal considerations alone.

Consider the case in which the temperature varies exponentially. (It is assumed that the variation of temperature while heating under constant load can be represented by *one* exponential curve, characteristic of the particular motor, and similarly for cooling.) For economic utilization of the thermal properties of the insulation, since the temperature now varies with time, a generalization of equation 3 may be written as

$$\int_0^1 A\epsilon^{+a(\phi-\theta)}\,dt = A \times 1.0 \text{ (where } \phi \text{ is instantaneous temperature)} \quad (5)$$

or

$$\int_0^1 \epsilon^{a\phi}\,dt = \epsilon^{a\theta}$$

whence (3) is obtained if $\phi = T$ = a constant for the fraction t_0 of the total cycle time.

The actual length of cycle time must now enter the calculations. Note that with a finite time-constant (for heating or cooling) as the cycle time approaches infinity the conditions of the previous case (instantaneous heating and cooling) are approached. In order to include the cycle time, it will be convenient to define two dimensionless quantities: α equal to the ratio of the total cycle time to the motor time-constant for heating and β equal to a similar ratio for cooling. The ratio of α to β will be denoted by γ. The steady-state temperature rise corresponding to the load applied toward which the actual temperature climbs while the motor is on will still be denoted by T. Then, since the temperatures at the beginning and the end of the cycle must be the same, it can easily be shown that during heating (motor on) the temperature variation is given by:

$$\phi = T(1 - k_1\epsilon^{-\alpha t}) \qquad 0 < t < t_0 \qquad (6)$$

and during cooling (motor shut down) by:

$$\phi = Tk_2\epsilon^{-\beta t} \qquad 0 < t < t_f$$

where

$$k_1 = \frac{\epsilon^{\beta t_f} - 1}{\epsilon^{\beta t_f} - \epsilon^{-\alpha t_0}}$$

$$k_2 = \frac{\epsilon^{\alpha t_0} - 1}{\epsilon^{\alpha t_0 + \beta t_f} - 1}\,\epsilon^{\beta t_f}$$

Equation 5 then becomes:

$$\epsilon^{aT}\int_0^{t_0} \epsilon^{-aTk_1\epsilon^{-\alpha T}}\,dt + \int_0^{t_f} \epsilon^{aTk_2\epsilon^{-\beta t}}\,dt = \epsilon^{a\theta} \qquad (7)$$

which reduces to (3) for $\alpha = \beta = \infty$. Fortunately, for the purposes of calculation equation 7 can be further reduced to a function of the "integral exponential" (E_i) for which tables of values exist. Thus:

$$\frac{\epsilon^{aT}}{\alpha}\left[E_i(-aTk_1) - E_i(-aTk_1\epsilon^{-\alpha t_0})\right] + \frac{1}{\beta}\left[E_i(aTk_2) - E_1(aTk_2\epsilon^{-\beta t_f})\right] = \epsilon^{a\theta} \qquad (8)$$

Although this decidedly is not an explicit relation for T as a function of α, β, t_0, and θ, by dint of successive trials, the representative curves of figures 11 and 12 may be drawn.

In brief, the calculations demonstrate that under usual ambient conditions and with a reasonable variation between actual heating and cooling time-constants of motors used on normal time cycles the effect of assuming instantaneous variation of temperature with load is more conservative than necessary.

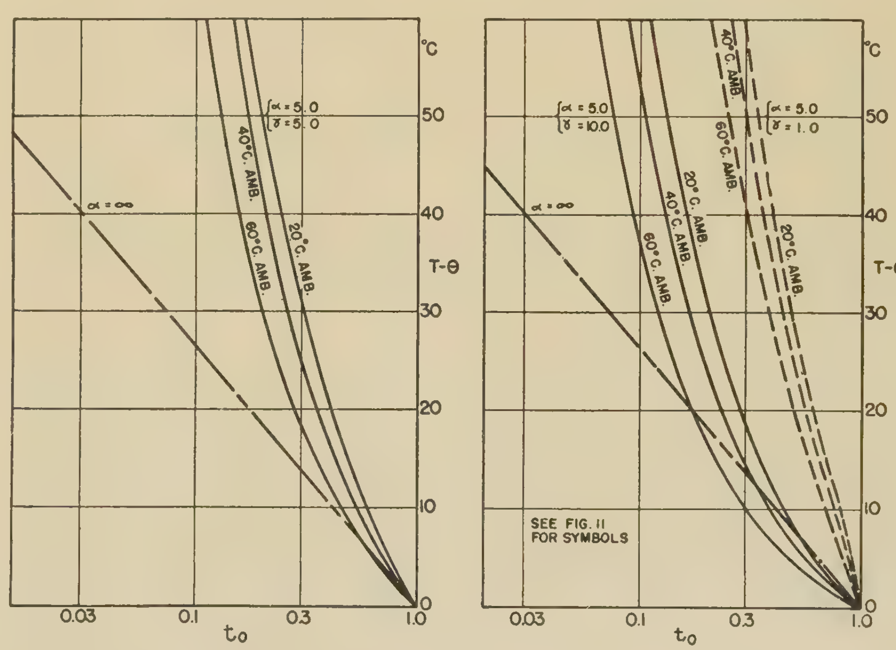
A typical small motor in the range from one-half to five horsepower will have a heating time constant of the order of ten minutes. With usual repetitive duty cycles of two hours or longer duration, therefore, the value of α will be of the order of 10 or more. The cooling time constant, with the motor at standstill is usually about five times as long as for heating. Thus, we may assume $\alpha = 10$ and $\gamma = 5$ as fairly typical values in service.

The effect of considering the thermal time lag of insulation is to allow a greater overload to be carried for the same duration or the same overload for a greater fraction of the total cycle time, as may be seen by referring to figure 11. For a temperature excess of $T - \theta = 25$ degrees centigrade (corresponding to 140 per cent current for a standard motor at 40 degrees centigrade ambient) a typical small motor ($\alpha = 10$, $\gamma = 5$) could permit an overload duration of 0.23. This is to be compared with the maximum duration of 0.33 which could be allowed if the cycle were so short that the temperature did not vary ($\alpha = \infty$), and to the value of 0.115 which would be recommended on the instantaneous heating basis ($\alpha = 0$) proposed in the body of the paper. The recommended values, therefore, make ample allowance for variations from usual conditions, and this margin decreases only slowly as the cycle time is increased. Figure 12 shows the relative effects of heating and cooling time constants. Increasing γ means increasingly slower cooling which, therefore, loses part of the advantage gained by the temperature lag while the motor was on. Anything which tends to equalize the rates of heating and cooling increases the allowable duration of overloads. A change from the typical condition of $\gamma = 5$ to equal rates of heating and cooling ($\gamma = 1$) for a temperature excess of $T - \theta = 25$ degrees ($\alpha = 5$, and 40 degrees centigrade ambient) increases the allowable duration from 0.31 to 0.50, as compared with the recommended value of 0.115. The extreme case of $\gamma = 10$, which shows some decrease in allowable time from the recommended values is quite unlikely in practice.

Offsetting this gain from time lag in heating is the temperature creep due to increased copper resistivity at elevated temperatures. From figure 11, for a typical case of $\alpha = 10$, $\gamma = 5$, 40 degrees centigrade ambient, thermal lag increases the allowable temperature rise for a cycle 15 per cent on and 85 per cent shut down, from 84 degrees centigrade to 105 degrees centigrade, or by a factor of 1.25. On the other hand, figures 4 and 7 indicate that on a continuous load, allowance for creep requires that the expected rise be set at only 93 degrees centigrade, if 105 degrees centigrade actual rise is to be obtained. Thus, the effect of temperature creep largely offsets that of thermal lag for high overloads and long cycles.

By this analysis, therefore, simple methods have been derived for determining the limiting permissible overload duration for any heating constants and any length of duty cycle, making use of figures 11 and 12. The results indicate, however, that the simple curves of figures 2 and 5 are adequate for most purposes, the additional overload capacity due to thermal time lag being of importance only for duty cycles of two hours or less.

Figure 12. Permissible excess temperature rise in intermittent service, for fixed insulation life (showing effect of cooling time constant compared to heating time constant)



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Discussion

L. A. Kilgore (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors are to be commended for a scientific attack on this problem and their ultimate simplification of some involved data and theory. The conclusions, of course, are no more accurate than the original assumed relation between temperature and life of the insulation. While this data seems fairly well confirmed from experience with actual machines, it would seem somewhat optimistic based on the results of the paper, "Temperature Life Characteristics of Class A Insulation," by J. J. Smith and J. A. Scott.

Since the paper refers in several places to 15 horsepower and up, the inference might be drawn that the typical curves and data given applied equally well to machines above 200 horsepower. I am sure the authors did not intend this, for while the general principles given are applicable, still the typical values are different. For example, the typical curves, figure 3, show 180 per cent starting torque, which is several times too high for general-purpose class I motors.

L. E. Hildebrand (General Electric Company, Lynn, Mass.): There is a point common to my paper and that of Messrs. Alger and Johnson which should be explained. I refer to the per cent time on for intermittent cycles. The same thing is said in both papers. However, one who reads them hurriedly without noting a change in viewpoint may hastily infer that we do not agree. Their paper shows the per cent time on for various loads based on an ideal long cycle. This might be called short year service, meaning that the motor is operated a few months per year or a few days per month, but when it does operate, it reaches and maintains continuous temperature for a long time. In their appendix, they show corrections for shorter cycles.

In my paper I consider ideal short cycles to which they refer in the section "Permissible Intermittent Overloads." In the ideal short cycle, the on and off periods are so short and so frequent that the temperature does not rise above the average. The ideal short cycle essentially describes machine-tool jobs. It is expedient and clearer to consider such jobs from the root-mean-square load standpoint. If the cycle becomes so long that the temperature does rise a few degrees above the average, a simple correction can be made. Hence if the cycle is hours per day, use long cycle methods corrected for thermal log. If the cycle is minutes per hour, use short cycle methods corrected for temperature variations. Most machine tool cycles are so short that no correction is necessary.

The permissible "time on" for short-cycle jobs is much greater than for long-cycle jobs. A motor with proper winding will carry about 140 per cent base load, 30 per cent time on if the cycle is long, 66 per cent time on if the cycle is short. 175 per cent base load, 6 per cent time on for a long cycle becomes 43 per cent time on for a short cycle.

The motor described in the section "Characteristics of Motors Designed for Intermittent Service" by Messrs. Alger and Johnson is very close to the one-hour motor of the next higher standard rating if it refers to the lower horsepower range. It is good for 175 per cent base load, 6 per cent time on ideal long cycle. The more common application is say 130 per cent base load perhaps less, on 80 per cent or even less running idle between load periods starting every few minutes. The extra torque ability is insurance that the machine tool will do the infrequent abnormally severe job or that the motor will operate the machine during some short peak load which is distinctly higher than the average or the root-mean-square load. Thus, this motor is ideally suited to the majority of machine tool and similar jobs.

H. L. Wallau (Cleveland Electric Illuminating Company, Cleveland, Ohio): The authors are proposing a method of motor rating which will combine available starting and breakdown torques with thermal limits to yield what they consider a reasonable service life of ten years for either continuous or intermittent duty, and that stator-winding temperatures be measured by resistance. These proposals seem to have logic behind them, although whether a life as short as ten years is proper may well be open to question. The introduction of a service factor permits the continuing use of the nominal ratings to which industry is accustomed.

In their final conclusion the authors state in part, "the temperature-life curve of figure 1 . . . should be reviewed . . . before final values for standards are accepted." This is very important inasmuch as the test curves from which curve B, figure 1, is replotted, do not yield a probable life expectancy of seven years for continuous operation at 105 degrees centigrade, and the use of this seven-year expectancy is basic to the values set up in the paper.

The curves from which Montsinger's figure 11 is derived, are shown in figure 6, page 783, April 1930, AIEE Transactions. The three curves numbered 4, 5, and 6 in this figure when plotted on semilogarithmic paper, with time to the logarithmic scale, and extrapolated to zero tensile strength, show relative temperature-life expectancies under oil, as follows:

Temperature (Degrees Centigrade)	Approximate Life Weeks	Approximate Life Years
90	900	18
100	360	7
110	170	$3^1/_4$

Figure 11 as erroneously plotted indicates a life expectancy at 100 degrees centigrade of 11 years. For 105 degrees the correct indicated expectancy is of the order of five, not seven years. The plotting error was called to Montsinger's attention recently, as these data are reproduced on pages 261 to 263 of "Transformer Engineering," and the discrepancy was acknowledged.

A study of the individual curves of Montsinger's paper (figures 6 and 7) does not seem to warrant the statement that insulation in air will outlast insulation under oil. Curve 3 of figure 6 has a straight-line characteristic with a loss of tensile strength of one-half per cent per week, indicating an ultimate life of 200 weeks. This is for varnished cambric in air at 110 degrees centigrade. Curves 1 and 2 of the same figure for temperatures of 90 degrees centigrade and 100 degrees centigrade, respectively, have convex characteristics indicating a shorter life at these lower temperatures, and all three indicate shorter expectancies than the corresponding curves for this insulation under oil. Figure 7 shows similar discrepancies. It would appear that too few test data were available to Montsinger at that time to obtain results consistent with experience.

Much caution must be exercised therefore in the selection of the proper life expectancy of insulation before either standards or operating guides are formulated for industry, and it is quite possible that sufficient knowledge on this particular subject is not yet available.

Hubert H. Race (General Electric Company, Schenectady, N. Y.): The time-temperature relation of the deterioration of insulating materials is a very important problem to the designer and user of electrical apparatus and rightfully deserves the emphasis that has been given it at this convention. *Often the physical changes which make such deterioration evident are really indications of chemical changes which caused them, so that physical evaluations of deterioration are insufficient but should be accompanied by controlled chemical experiments.* Therefore it seems important to contribute to this symposium a short engineering statement of

Table I. Ratios of Rates of Chemical Change for Ten-Degree-Centigrade Intervals for Different Values of Heats of Activation (Q)

Q (Calories per Mol)	t_1 (Degrees Centigrade)	t_2 (Degrees Centigrade)	(k_1/k_2)
15,000	150	140	1.54
15,000	130	120	1.61
15,000	110	100	1.70
20,000	150	140	1.78
20,000	130	120	1.89
20,000	110	100	2.02
25,000	150	140	2.06
25,000	130	120	2.21
25,000	110	100	2.42

our knowledge of the general law expressing the temperature dependence of various physical and chemical phenomena. The most general statement of this law is

$$k = A\epsilon^{-\frac{b}{T}} \tag{1}$$

This general relation holds for phenomena such as chemical reactions, chemical equilibriums, evaporation, temperature coefficients of viscosity, and electrical conductance in semiconducting crystals. For chemical reactions, the law was given by Arrhenius in the form

$$k = A\epsilon^{-\frac{Q}{RT}} \tag{2}$$

where

k = a constant of a differential equation which defines the rate of the chemical reaction
A = an experimental coefficient
Q = the heat of activation in calories per mol
R = the gas constant = 1.986 calories per degree mol
T = absolute temperature in degrees Kelvin

Since the coefficients of the above equations have considerable significance from the general background of our theoretical and experimental knowledge, wherever possible new data showing temperature dependence of any kind should be examined in terms of this general relation. This can be done by plotting log (dependent variable) against ($1/T$), which should give a straight line whose slope has physical significance.

Taking the logarithm of the ratio of k in equation 2 for two temperatures, we have

$$\log_\epsilon (k_1/k_2) = (Q/R)\ [(T_1 - T_2)/T_1T_2] \tag{3}$$

An approximate rule-of-thumb statement is that the rate of a chemical reaction doubles for a 10-degree-centigrade increase in temperature. This rule is illustrated in table I of this discussion, in which are given the ratios (k_1/k_2) of the rate constants of chemical reactions calculated from equation 3 for three sets of 10-degrees-centigrade intervals at each of three values of Q. For most chemical reactions, within the temperature range in which we are interested, Q lies between 15,000 and 25,000 calories per mol.

Equation 2, which has a large mass of chemical data supporting it, is different in form from the empirical relation given in equation 2 of the paper on "Rating of General-Purpose Induction Motors" by Alger and Johnson. The coefficients of their relation have no theoretical significance. However, by expanding equation 2 above in a series for ($T - \theta$) it can be shown that neglecting second order terms gives the form of Alger and Johnson's equation 2, and that for the range of temperatures being considered the error in this approximation is less than five per cent. Therefore the two relations are equivalent within the experimental error.

Although the general validity of the above rule for chemical reaction rates is unquestioned, in any given insulation system a number of other conditions must be known before an estimate can be made of the effects of such chemical changes on insulation life. In service, continuous chemical changes may (a) improve, (b) have no effect upon, or (c) decrease the electrical quality of insulation, depending upon conditions. Examples of these three possibilities are numerous and may result from many different types of chemical change depending upon particular conditions, but for illustration a single reaction, that of continuous curing of varnish in stator coils in service, may give all three results in sequence.

Stator coils, to be assembled, must be flexible. Therefore the varnish used in making them is not completely cured to a hard rigid form. In service the chemical and physical changes called "curing" continue slowly, resulting in continuously improving electrical quality for some time. These gradual changes continue throughout the life of the insulation and even though they progress to the extreme where the varnish is very hard, brittle, and full of cracks they may not cause electrical failure if, for example, the apparatus is not exposed to moisture, and the basic insulation such as mica is not disturbed by vibration or excessive expansion and contraction. I helped to dismantle several of the original two-phase generators at Niagara Falls which were removed from service without failure, in which the organic insulation had been changed almost to dry dust but the machine had continued to operate as long as it was undisturbed. On the other hand these same changes might lead to early failure under more adverse conditions of service such as might be experienced by motors in mines or on shipboard.

The points I wish to emphasize in this discussion are: (1) chemical changes often underlie observed physical changes; (2) for this reason laboratory experiments on physical and electrical deterioration should be accompanied by chemical tests to determine causes; (3) experimental data showing temperature dependence should be examined in terms of equation 1 instead of being expressed in some other purely empirical form; (4) numerous specific conditions must be known before a conclusion can be reached as to the effects of such chemical changes on insulation life in any particular instance.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): I wish to discuss this paper from the broad question of name-plate rating versus output. My work has, of course, been concerned mostly with transformers in which temperature is the only limit to output.

AIEE Standards No. 13 at one time had the statement that the name-plate rating of transformers should not be exceeded whatever be the ambient. It is difficult to see how such a rule could be justified, but apparently some excuse for it was given. After getting rid of this rule in 1922 the way was open to recognize that name-plate rating and output may be entirely different. One of the first things we did in the rules was to permit one per cent overload continuously for each degree centigrade that the ambient is below the standard. On the other hand, if the ambient is continuously above the standard, permissible output was reduced accordingly.

This did not go far enough to meet the needs where short-time overloads (in excess of those permitted by the one per cent rule) are sometimes required to meet emergency conditions.

The next step was to set up permissible short-time overloads, ranging from short circuit of a few seconds to several hours time. To do this, however, required knowing, at least in a rough way, how short-time high temperatures affected the life of insulation. The tests reported in my 1930 paper showed that each time the temperature is increased eight degrees the rate of aging, of insulation immersed in oil, doubles.

Some of the data presented at this convention indicates that the eight-degree rule is too conservative; that is, it requires more than an eight-degree increase in temperature to double the rate of aging in air. I understand that someone in the Petroleum Institute presented a paper some time ago which showed that the deterioration of petroleum was doubled for each 8.5-degree-centigrade increase in temperature. I note that Messrs. Alger and Johnson used the eight-degree rule in their paper which, I feel, marks an important milestone in this question of rating versus output of rotating machinery.

I am not so much concerned in whether the eight-degree rule is absolutely correct, as I am in getting this fundamental method of analysis applied to electrical apparatus where temperature is the principal limit to output.

I do not want to give the impression that I am advocating that short-time overloads on rotating machinery be limited by temperature alone, as we have done for transformers, because there are limitations other than temperature for most classes of rotating machinery.

I do feel, however, that it is possible to go quite a ways yet in working out safe moderate overloads, as Messrs. Alger and Johnson have done. I venture the prediction that in a few years the name-plate rating of a large part of smaller rotating machinery will be used mostly for purchasing purposes, while the output will be more or less than the rating, depending on the operating conditions—the same as is true for transformers today.

C. G. Veinott (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Messrs. Alger and Johnson recognize the same difficulty outlined in Mr. Rutherford's current paper ("Rating and Application of Motors for Refrigeration and Air-Condi-

tioning Systems," AIEE TRANSACTIONS, volume 58, 1939, see 1939 annual TRANSACTIONS index for page numbers) that the present horsepower ratings, particularly of fractional-horsepower induction motors, do not adequately describe the motor. Their proposal is to increase the service factor and leave the present horsepower ratings intact. This proposal would undoubtedly be acceptable to the refrigeration manufacturers since it effects no change in ratings, but does little to clear up existing confusion as to control and wiring. In general, the torque and current specifications given by the authors are acceptable as they are conservative values for present-day commercial capacitor-start motors. But these standards can not universally apply to split-phase motors. However, we see little point in reducing the locked-rotor current specifications for the one-eighth and one-sixth-horsepower sizes.

The whole subject of locked-rotor currents is one which may well concern AIEE whose membership includes the power companies who are as vitally interested in this subject as well as the motor manufacturers. The great disparity of requirements in this respect was brought to light by a recent survey of 51 leading public utilities in the United States. The maximum permissible locked-rotor current for a three-fourth-horsepower 220-volt single-phase motor allowed by each company was tabulated with the following results:

1 company specifies 12.25 amperes maximum
16 companies specify 20 amperes or less
29 companies specify 30 amperes or less
The National Electrical Manufacturers Association specification is 30.5 amperes
22 specify more than 30 amperes (of these 22, 13 have no specifications)

In other words, 29 of 51 companies have not accepted the value established by NEMA as the lowest value to which motor manufacturers can work without penalizing the motor design unnecessarily. Fortunately many of these 29 companies do not enforce their locked-rotor current rules. The few that do often work a hardship on the motor user, forcing the motor manufacturer to design and build special motors for certain localities. This procedure usually involves expense and inconvenience both to the manufacturer of the motor and the builder of the appliance, generally resulting that the customer who buys the motor-operated appliance has to pay more for a less satisfactory appliance than a more fortunate customer who is permitted to use the standard motor. Some standardization is urgently needed in order not to obstruct progress in the development of motor-driven appliances, and the resultant general increase in use of electric power.

T. C. Johnson: Two important ideas which are the natural outgrowth of the paper on "Rating of General-Purpose Induction Motors" just presented arise for discussion at this time. The first concerns the available torque of the general-purpose motor and the second concerns the relation between the service factor, frame size, and the ten-degree temperature rise margin.

The available torque of general-purpose motors is very important and therefore it is fitting to re-emphasize this point as made in the paper.

Under the competition of good design and the guidance of AIEE, NEMA, and ASA standards, a method of rating has grown up which specifies that a motor operated continuously at rated load under the most severe conditions will have a temperature rise of 40 degrees. The standards also specify that the motor must be capable of delivering 115 per cent of its rated output continuously without damage to itself and that it must have a maximum torque at breakdown of not less than 200 per cent of rated torque. Since due allowance must be made for possible voltage reduction and unexpected variations in the application, good practice requires that not more than 135 per cent of rated torque be demanded. However, all of this torque is available and should be used for economic application although the extent to which this overload torque can be used depends on the application since temperature rise is an additional limitation.

Considerations of available torque lead to a discussion of the service factor and its relation to the rating of successive frame sizes. The service factor is most simply defined as

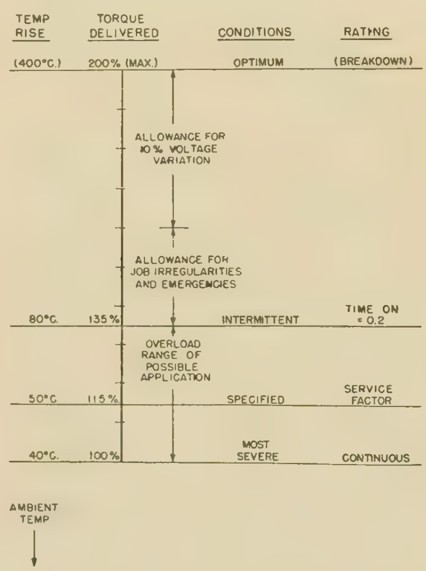

Figure 1. Available torque of integral-horsepower motors (15–200 horsepower)

the ratio of the maximum continuous load that can safely be handled to the rated continuous load. Under the present standards this corresponds to the ratio of the output with a 50-degree rise to the output with a 40-degree rise. The possible difficulties due to the service factors increasing in size for the smaller-horsepower motors, as shown in the paper, may be removed by considering the service factor as a simple measure of the step in rating between successive frame sizes. For integral-horsepower motors which have an average step in rating of 30 per cent between successive frame sizes, the service factor, defined as before, is equal to 1.15 and corresponds therefore to a step of one-half frame size in rating.

For fractional-horsepower motors the case is very similar. A small motor has higher breakdown torque than a large motor for the following reasons: (1) the steps in rating between frame sizes are larger in order to cover the range economically, (2) the uncertainties in application are a greater percentage of the output, (3) the power factor is naturally poor, (4) applications are most commonly intermittent.

For these reasons the service factor, again defined as the 50-degree rating over the 40-degree rating, for small motors is equal on

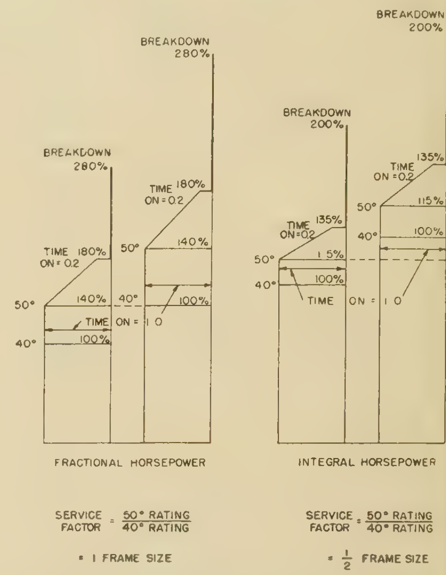

Figure 2. Relative output of successive frames

the average to 1.4. Since the average step in frame size horsepower for small motors is about 40 per cent, the service factor may most simply be thought of as a step of one frame size in rating.

These two points of available torque and service factor are summarized on two figures. In the first figure the bottom line represents operation at rated output, 40-degree rise, 100 per cent load, continuous service under most severe conditions. The next line above represents operation at the service factor rating, 50-degree rise, continuous operation. The top line represents the breakdown torque. Making allowances as shown for voltage variation and mechanical emergencies, there is left 135 per cent rated torque to be used. For a load which is on only two-tenths of the total cycle time, this full 135 per cent torque can and should be used for economic application.

On the second figure is shown the relative output of successive frame sizes, first for integral-horsepower motors where the 1.15 service factor corresponds to an increase in rating of one-half frame size and second for fractional-horsepower motors where the average service factor of 1.4 corresponds to a step of one whole frame size. The relative widths in the diagram correspond to the approximate times for which the corresponding torque can be utilized with safety. Thus the service factor may very conveniently be related to the step in rating between successive frame sizes.

P. L. Alger: We appreciate the comments of Messrs. Race and Montsinger, who agree with our use of the simple law of about half life for each eight-degree increase in temperature, as a practical way of expressing the effect of temperature on insulation. The actual life obtained on any given motor depends in a very high degree on the particular conditions met with, such as the

ambient temperature, the load cycle, and especially the degree of dirt, moisture, vibration, etc. The use of the law is rather in estimating the effect of a temperature change on insulation life, found by experience in particular conditions, than in estimating the life under totally new conditions.

Answering Mr. Wallau's statement that our curve *B*, figure 1, is not an exact representation of Mr. Montsinger's 1930 test data, the curve does represent an over-all average of experience, of which the particular tests reported by Mr. Montsinger are only a part. The important conclusion is that the 105-degree-centigrade limiting hot-spot temperature for class *A* insulation has been found generally satisfactory in industrial service.

In reply to Mr. Kilgore's comment, the curves in the paper are intended only to cover general-purpose motors—that is, 200 horsepower and smaller at 450 rpm and higher speeds. The starting torques are assumed to correspond to the NEMA standard of 150 per cent of full load torque for four-pole motors. The 180 per cent value for the 25-horsepower motor shown in figure 3 provides a reasonable margin over this 150 per cent guaranteed figure.

Mr. Veinott points out the wide variation in starting-current requirements of different power companies and emphasizes the importance of having such adequate standards that the motor name-plate conveys a definite description of the motor's performance. This is very pertinent, as, under present conditions, many small motors are operated at loads greatly in excess of their name-plate values, and this leads to a great deal of uncertainty about the proper control and wiring.

There are two possible ways of procedure. One is to have a great number of special motors designed for particular services, each with a name-plate horsepower and time period of rating exactly representative of the expected motor performance. Such motors would naturally have a low ratio of maximum to full-load torque and would have a relatively low starting current per rated horsepower. The other procedure is to have a limited number of special motors of the foregoing closely rated types, and in addition to have a standard general-purpose type of motor suitable for operating under any one of a wide range of conditions. Such general-purpose motors, however, would naturally be used at widely different values of horsepower, depending on the intermittency of load, the ambient temperature, the desired life, and other conditions. If such motors are used, it is logical to give them a single conservative rating, representing the output the motor can always be relied upon to deliver under severe conditions; and, in this case, such motors will have relatively high starting and breakdown torques, high starting currents, and considerable overload ability, which will be used in many applications. The permissible starting current should, therefore, be based on the service-factor rating of the general-purpose motor, to be consistent with the rules for closely rated special-purpose motors.

In conclusion, I wish to point out again that this paper is intended to give a better understanding of the range of possible application of a standard general-purpose motor which, without change in name plate, is admirably fitted to a wide range of uses.

Determination of Temperature Rise of Induction Motors

E. R. SUMMERS
ASSOCIATE AIEE

Synopsis: The "American Standards for Rotating Electrical Machinery" (American Standards Association) prescribe that the temperature rise of motors shall be measured by the thermometer method for purposes of rating. Definite limiting values of temperature rise are established for each type of machine. The AIEE Standards No. 1 fix conventional allowances of 15 degrees centigrade, 10 degrees centigrade, and 5 degrees centigrade between the actual hottest-spot temperature and the highest observable value of temperature as determined by thermometer, resistance, and embedded-detector methods, respectively.

This paper presents information on the relations between the measured values of temperature rise by different methods as found in tests on several hundred induction motors ranging from 10 to 1,000 horsepower in rating. It is shown that, with modern motor construction, variations of 20 degrees centigrade or more are sometimes obtained by the thermometer method on a given machine depending on location of thermometers or thermocouples, whereas the resistance measurements give relatively consistent values of temperature rise.

It is therefore suggested that the standards for temperature-rise measurements be revised, and that the resistance method be adopted for all forms of enclosed or protected machines which are not readily accessible for application of thermometers on laminations, insulated windings, and other adjacent parts.

TEMPERATURE rise is more frequently a limiting feature than is any other single motor characteristic in determining the maximum horsepower ratings that may be obtained from a given induction-motor frame size. Dependable heating data are of primary importance, because the probable length of insulation life diminishes rapidly at excessive winding temperatures. Procedures for obtaining motor temperature measurements are not well standardized at the present time, and the reliability of heating data cannot be satisfactorily evaluated unless the fidelity and accuracy of testing methods are known.

Paper number 39-3, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 21, 1938; made available for preprinting November 25, 1938.

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The objects of this paper are (1) to suggest revision of the present induction-motor standards to place temperature measurements on a better basis, (2) to present test data which illustrate the need for and justification of these revisions, and (3) to suggest new temperature ratings of motors on basis of using the resistance method of determining temperature rise.

AIEE Standards No. 1 dated April 1925, recognize three fundamental methods of temperature determination which are defined respectively as the thermometer, resistance, and embedded-detector methods. Rules governing the interpretation of these different methods are quoted from paragraph 1–7 of the standards:

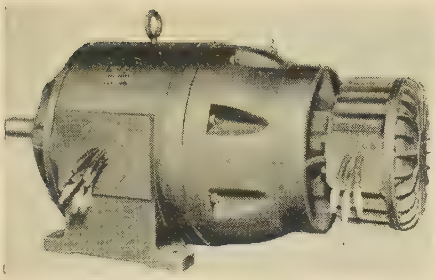
1–7 Limiting Observable Temperatures and Conventional Allowances. Limiting "observable" temperatures are deducted from the limiting "hottest-spot" temperatures by subtracting therefrom a specified number of degrees which, For Purposes of Standardization, is the margin fixed between the limiting hottest spot and the limiting observable temperatures.

This margin is designated as the "Conventional Allowance."

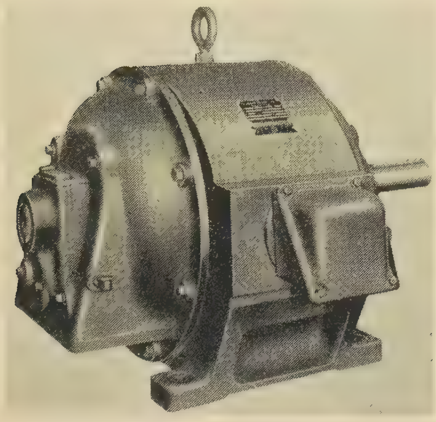
The specified differences (which may be designated the "Conventional Allowances") by which the "observable" temperatures are, For Purposes of Standardization, assumed to be lower than the "hottest-spot" temperatures, are as follows:

Thermometer Method—
15 degrees centigrade
Resistance Method—
10 degrees centigrade
Embedded-Detector Method—
5 degrees centigrade

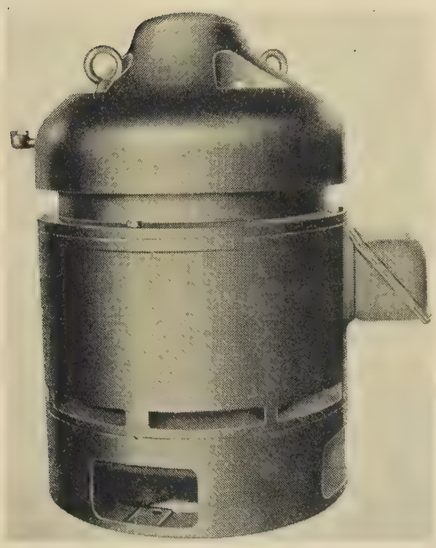
On the basis of the standard 105-degree-centigrade limiting "hottest-spot" temperature, the maximum "observable" temperature must not exceed 90 degrees centigrade by thermometer for class *A* insulation; and from this were derived the present limiting ratings of 50 degrees centigrade rise by thermometer above the standard 40 degrees centigrade ambient for special-purpose open motors, and 40 degrees centigrade rise combined with a 1.15 service factor for general-purpose open motors. The "conventional allowances" for "hottest-spots"



(A)



(B)



(C)

Figure 1. Typical induction motors of modern construction which are not readily accessible for thermometer measurements

(*A*) Totally enclosed fan-cooled wound-rotor type, (*B*) splashproof type, and (*C*) vertical motor for outdoor service

have generally been considered as five degrees centigrade less for totally enclosed than for open motors because the windings of the former are heated more uniformly, and from this premise the maximum rating of 55 degrees centigrade rise by thermometer above a 40 degrees centigrade ambient was adopted for totally enclosed motors.

Paragraph 5.050 of the ASA *C*-50 standards approved on January 6, 1936 (subsequently to adoption of AIEE Standards No. 1) specifies that all temperatures of induction motors shall be determined by thermometer method. ASA rules 2.055 and 2.063 relative to thermometer method are as follows:

2.055—*Thermometer Method of Temperature Determination Defined.* This method consists in the determination of the temperature by mercury or alcohol thermometers, by resistance thermometers, or by thermocouples, any of these instruments being applied to the hottest part of the machine accessible to mercury or alcohol thermometers.

2.063—*Measurement of Machine Temperatures.* As far as practicable, temperature measurements shall be taken during the test, without making any change in the machine which will affect the results, as well as immediately after shutdown. The temperature of totally enclosed motors (including totally enclosed fan-cooled motors) shall be obtained after shutdown by applying the thermometer to the hottest part of the machine which can be made quickly accessible by removing covers. If the core and windings are not accessible such as in totally enclosed or totally enclosed fan-cooled motors, holes must be provided through any intervening structural parts to permit the application of the thermometer method.

The highest measurements obtained shall be the accepted values.

The location of the hottest observable point in a motor actually depends on a number of factors such as the enclosing features if any, type of winding and insulation treatment, arrangement of fans and air deflectors, number and size of air ducts, relative proportions of component losses, etc. No standardized thermometer locations would be suitable for all types of motor construction. Consequently the test results by thermometer method may or may not be representative of actual maximum temperature rise depending on familiarity of testing personnel with motor characteristics, and also on whether a conscientious effort is made to locate the hottest observable point in the machine. The term "accessible" is also subject to individual interpretation and cannot be sharply defined.

A different arrangement of thermometers may occasion a wide disagreement in test results when the same motor is tested by two different parties, and these variations are frequent sources of controversy. The testing situation is becoming increasingly complicated because of the definite trend toward more enclosing features in the design of induction motors. A large percentage of modern motors now has a mechanical construction which either reduces or eliminates entirely the accessibility of internal parts as illustrated by figure 1.

To clarify this unsatisfactory situation with respect to temperature measurements, testing methods should be modified in accordance with changes in motor construction, so that comparable results may be obtained when the same machine is tested by two different organizations. This paper recommends general recognition, adoption, and standardization of the resistance method. Results of many tests are presented to prove that the winding resistance gives a more reliable indication of actual insulation temperature than measurements made with thermometers.

Some Specific Situations for Which the Present ASA Standards Are Inadequate

Specific test results and situations are presented to illustrate the inadequacy of the present ASA standards relative to heating tests.

1. Totally Enclosed Fan-Cooled Motors

Reliable winding temperature data usually are not obtained by thermometers inserted through holes bored in the frame of an enclosed motor as directed by ASA rule 2.063.

A test made on the totally enclosed fan-cooled motor designated as *X* in appendix I and table I, where the stator winding was explored by placing 28 thermometers and thermocouples at different points as indicated in figure 2, shows variations of 22 degrees centigrade (27 degrees centigrade to 49 degrees centigrade) in temperature rise, on the surfaces accessible to thermometers. A maximum variation of 32 degrees centigrade (27 degrees centigrade to 59 degrees centigrade) is obtained by including the inside surfaces of coil ends which are accessible to thermocouples but not to thermometers inserted through holes bored in the frame.

2. Multiwinding Motors

On multispeed motors having more than one stator winding, thermometers usually cannot be placed on the underneath winding. The lower-speed winding is almost invariably located inside next to the air gap. Because of reduced ventilation this lower-speed winding usually has the higher temperature rise, especially on constant-horsepower and constant-torque ratings. Thermometer measurements taken on the outside idle

winding may be from 10 degrees centigrade to 20 degrees centigrade lower than the actual temperature of inside active winding. Confirming tests are described in appendix II, and the test data are presented in table II. With the "sandwiched" type of coil construction, the inside winding may not be accessible for either thermometers or thermocouples. The ASA standards do not have any satisfactory provisions for testing multiwinding induction motors. On open as well as enclosed multiwinding motors, only one winding is usually accessible for the thermometer method; consequently this criticism of the present standards applies to all types of mechanical construction.

3. Wound-Rotor Machines With Inaccessible Rotors

No provisions are made in ASA standards for rotor temperature measurements of totally enclosed wound-rotor motors of the form shown in figure 1. With this construction the rotor winding is usually the limiting feature, because its temperature rise is commonly 10 degrees centigrade to 20 degrees centigrade above that of the stator. On all forms of totally enclosed as well as on most splashproof wound-rotor machines, the insulated rotor winding is completely inaccessible to the thermometer method when outside enclosed collectors are used. The provision of paragraph 2.063 of ASA standards for "applying the thermometer to the hottest part of the machine which can be made quickly accessible by removing covers" is certainly not applicable where it becomes necessary to practically disassemble the machine in order to gain access to at least one end of the rotor winding. Such a major disassembly of large machines requires so much time that subsequent temperature readings are of little significance. In field tests it may not be possible to disassemble the motor.

4. General-Purpose Motors

Although general-purpose motors with open-type construction are more accessible for application of thermometers on the windings and laminations than the types of machines cited in the preceding paragraphs, nevertheless, there is considerable variation in the results which may be obtained by different parties due to varying locations of thermometers. Appendix IV and table IV present results of tests made on motors of 12 different manufacturers, and five of these machines showed a temperature rise higher than the name-plate stamping. It, therefore, seems most probable that this situation is a result of variations in testing procedure which may now occur under the present standards.

5. Explosion-Proof Motors

Although the Underwriters do not actually forbid the boring of holes for thermometers in explosion-proof motors, such a practice is dangerous and undesirable; because any failure to plug a hole securely might result in an explosion external to the motor after the machine is placed in normal service. The present standards as worded therefore may create a potential hazard when tests are taken on explosion-proof machines.

Summary of Test Results by Resistance Method

To investigate and demonstrate the dependability and consistency of the resistance method of measuring temperature rise, an analysis was made of approximately 300 heating tests on various types of polyphase induction motors over the range of sizes from 10 to 1,000 horsepower. Temperature rises were measured both by the resistance method and also by thermometers (or thermocouples). The test results may be summarized as follows:

1. On totally enclosed and totally enclosed fan-cooled motors, the stator temperature rise by resistance method is essentially the equivalent of that obtained by thermocouples applied at hottest part of winding surface. On the basis of averages, the thermocouples checked the resistance method within 1.1 degrees centigrade (48 degrees centigrade–46.9 degrees centigrade) for the 65 tests on totally enclosed fan-cooled motors, and within 0.6 degrees centigrade (40.1 degrees centigrade–39.5 degrees centigrade) for the 26 tests on totally enclosed motors which are tabulated in table III. To locate the hottest winding surface, some of the thermocouples were placed at points which were not accessible to thermometers inserted through holes in stator frame as explained in appendix III.

2. On open-type or partially enclosed splashproof (or dripproof) machines rated 2,200 volts or less, heating tests on 149 different motors as described in appendices III and IV indicate about two degrees centigrade less average stator temperature rise by the resistance method than by thermometers (or thermocouples at locations which could have been reached by thermometers through holes in the frame) searchingly applied in accordance with the present ASA standards. (The 149 tests include the stator temperature measurements on the 41 splashproof and 96 open motors of table III and on the 12 open motors of table IV.)

3. Measurements taken on the 36 insulated wound-rotors of table III show an average temperature rise of 4.6 degrees centigrade (34 degrees centigrade–29.4 degrees centigrade) more by change in resistance than by thermometer. Lower and in general less accurate values of temperature rise are obtained with thermometers because: (*a*) thermometers cannot be placed on center of rotor laminations in most cases, (*b*) the rotor must stop turning before thermometers can be applied and they do not attain maximum indication until after resistance readings are taken, and (*c*) rotors of many motors are not readily accessible and good thermometer contacts cannot always be obtained.

4. The resistance method is consistently reliable for all speeds and time ratings. In table III the results are segregated according to motor poles over the range from 2 to 12 poles for the continuous-rated squirrel-cage motors. Heating runs of one hour or less are separated from the tests of longer duration.

5. The resistance method is dependable over a wide range of motor load and line voltage as demonstrated in appendix V, table V, and figure 4. Tests on a representative 25-horsepower motor for different conditions of operation show that the temperature rise by any method is approximately proportional to the total motor losses.

6. Maximum differences of about ± ten degrees centigrade between the thermometer and resistance methods have been observed in tests on individual machines. The resistance method may definitely indicate either a higher or lower temperature than thermometers depending on mechanical construction of motor, the two extreme conditions being:

(*a*). Motors with improperly proportioned ventilation may have local hot spots where

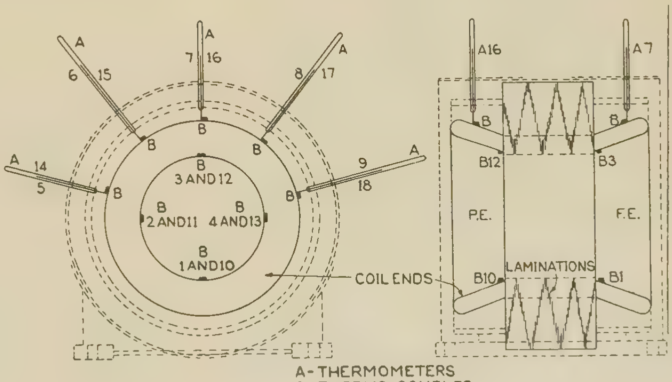


Figure 2. Location of thermometers and thermocouples on motor X

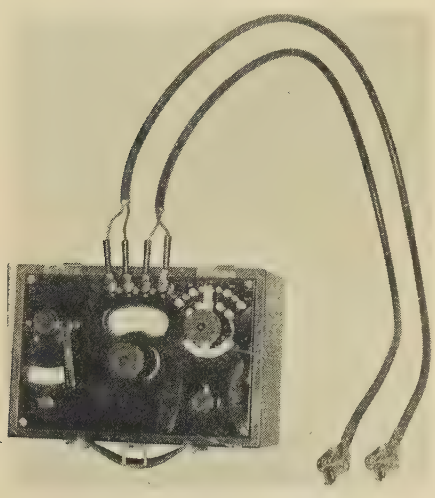
(Refer to appendix I and table I)

thermometer readings exceed resistance indication, but in many cases these hot spots are not located and explored by the thermometer method of test.

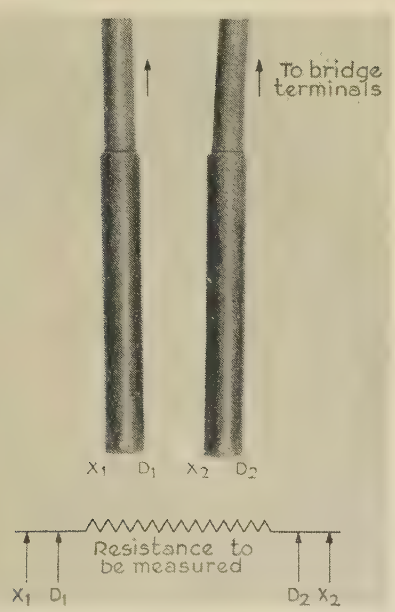
(*b*). High-voltage machines, especially 4,000 volts and above, usually (but not always) show a higher temperature rise by resistance than by thermometer because of the temperature drop in the coil insulation, in which case the resistance method is safer because it represents actual conditions.

Discussion

The thermometer method, as commonly used, is not a sufficiently definite procedure for determining the temperature rise of partially or totally enclosed motors, because it is probable that an insufficient number of thermometers will be used to locate the hottest point accessible to them. Wide variations in temperature may occur over a motor surface that appears to be uniform and symmetrical. The element of "chance" is therefore always associated to some extent with the thermometer method. On splashproof and other types of partially closed motors, thermometer readings may be as much as 10 degrees centigrade below the temperature of the hottest surface that might have been reached. Motor *K* of table IV in appendix IV is cited as a specific example wherein the actual maximum observable temperature rise of 61 degrees centigrade by thermometer was 11 degrees centigrade higher than the manufacturer's name-plate rating of 50 degrees centigrade—presumably because his test with thermometers did not locate the hottest spot.



(*A*)



(*B*)

Figure 3. (A) Portable double bridge with leads for stator resistance measurements and (B) prong-type contacts used on collector rings of wound rotors

On totally enclosed machines where it is necessary to drill a separate hole in the frame for each thermometer, only a few are likely to be used—sometimes only one, and this increases the probability that the hottest part will not be located. Furthermore when thermometers are inserted in holes bored through intervening parts, it is usually impossible to cover the bulbs completely with pads or putty as specified in paragraph 2.056 of ASA standards. If only the end of thermometer bulb makes a point contact with the winding or laminations, the reading may be influenced by the surrounding air to which the bulb is exposed. As a practical result of all these factors, the trend toward more enclosing features on motors has been accompanied by increasing variations in the test results that are obtained by the thermometer method. Motor *X* of table I in appendix I is a striking example.

As the present standards do not insure reasonably accurate and consistent results when various motor types are tested by different organizations, it is therefore highly desirable to adopt a more convenient, more definite, and less expensive method of measuring temperatures which will give an accuracy comparable to that which can be obtained by a thorough exploration with thermometers.

The test data point to the desirability of standardizing on resistance measurements for the determination of temperature rise. For practically all types of motor construction, the resistance method gives results that are essentially the equivalent of a careful exploration with thermometers. The resistance reveals the average internal temperature of the motor winding and thereby gives a more reliable indication of actual insulation temperature than a few thermometer readings taken at isolated points on the outside surfaces of the coils.

The relation between winding resistance and temperature is a specific, definite physical law that is entirely divorced from the element of chance which is inherently associated with the thermometer method. The winding resistance can be measured quickly, conveniently, and at little cost. From the standpoint of comparative tests, the precision of the resistance method is limited only by the accuracy of resistance readings and the measurements of initial and ambient temperatures. Consequently it is believed that a general adoption of the resistance method will promote greater accuracy in testing, will assure the motor user of greater insulation life, will reduce controversy by making it possible for different organizations to obtain comparable test results, and will provide the motor user with a convenient method for taking field tests.

Precedents have already been established for recognizing the resistance method. The International Electrotechnical Commission European Standards, the United States Navy Specifications 17*M*10, and an increasing number of commercial customers now either accept or require the resistance method of testing to prove temperature-rise guarantees.

Although the resistance method requires accurate measuring devices and precision in testing procedure, experience indicates that these difficulties are minor compared to the benefits obtained when testing motors that do not have open-type construction. Readily portable instruments have been developed and are now available which have an accuracy that is satisfactory for motor temperature measurements. At least 95 per cent of the resistance data for the heating tests included in this report were obtained with a portable-type double-bridge arrangement similar to that shown in figure 3. The remainder of the readings were taken with the voltmeter-ammeter method by passing direct current through the winding. The portable double-bridge has proved very satisfactory in general testing work. It is readily carried, accurate, rugged, has a wide range of measurement, and is not easily thrown out of calibration. A description of this general type of bridge is given in an article entitled "The Portable Double Bridge" by L. O'Bryan in the *General Electric Review*, volume 34, 1931, page 752.

In common with most indicating in-

struments, the probable per cent error of measurement decreases as the upper end of bridge scale is approached. Bridges covering a range from 0.0005 to 4.4 ohms, and having a maximum probable error of 0.4 per cent in ohms (or about one degree centigrade) at most unfavorable part of scale that must be used, and a minimum probable error of 0.1 per cent at upper end of scale, have been found suitable for temperature measurements of motors between 10 and 1,000 horsepower for which data are presented in this paper. Bridges of this type can be made with various scales and scale multipliers to adapt them to different horsepower ranges of motors. Even though cumulative errors of 0.4 per cent in both the hot and cold resistance measurements do cause an error of two degrees centigrade in temperature rise, this is small compared to the error commonly made when a thermometer is inserted through a hole bored in the stator frame of an enclosed motor.

Errors due to contact drop in current circuit are minimized by using four leads to connect the unknown resistance to bridge terminals as shown in figure 3. Leads X_1 and X_2 carry the current which flows through the unknown resistance whereas D_1 and D_2 are potential leads. The voltage between D_1 and D_2 does not include the contact drops at X_1 and X_2. Part A of figure 3 shows the spring-type clips which are used for stator resistance measurements. The two contact surfaces of each clip are insulated from each other to provide separate voltage and current contacts.

Wound-rotor measurements are obtained with the contacts in part B of figure 3. The sharp prongs X_1 and D_1 are held firmly against one collector ring while X_2 and D_2 are placed upon another collector ring. Prongs D_1 and D_2 are supported on flexible coil springs which depress under pressure and thereby permit all prongs to maintain contact even though they are not held at right angles with collector-ring surfaces.

The following fundamental precautions will minimize testing errors and insure dependable heating data by the resistance method:

1. Use a bridge which is accurate within 0.5 per cent on lowest part of scale that must be used.

2. Calibrate the bridge periodically and keep the leads in good condition. Make sure that contacts are clean, and check galvanometer when readings are taken.

3. The persons taking resistance measurements should use all necessary precautions to obtain and record accurate data. A good check on the accuracy of results is obtained by having two different persons take readings with separate equipments. The two resistance readings should agree within 0.4 per cent (or about one degree centigrade).

4. Take resistance readings every minute for about ten minutes after motor stops and project the curve to zero time.

5. When taking "cold resistance" before the heat run, never assume that the motor temperature is the same as the ambient. Always place thermometers directly on motor. The motor may have been moved recently from another part of factory where temperature was different, the machine may not have cooled off completely from some previous manufacturing or testing operation, or the ambient temperature may have changed suddenly because of an open factory door. Accurate cold temperatures are just as important as accurate resistance readings.

6. Always take both hot and cold resistance measurements between the same two stator terminals or the same two rotor collector rings to avoid errors due to slight unbalance in phase resistances. If nonuniform heating is anticipated because of unbalanced power supply or unsymmetrical design, record resistance measurements of each phase separately.

Suggested Motor Temperature Ratings for Resistance Method of Test

If the resistance method of measuring temperature rise is accepted as standard, the question arises as to what temperature ratings by resistance shall replace the present 40-degree-centigrade, 50-degree-centigrade, and 55-degree-centigrade ratings by thermometer for general-purpose, special-purpose, and totally enclosed motors, respectively. The conventional "hottest-spot" allowance differentials in AIEE Standards No. 1 would suggest that all these ratings should be increased 5 degrees centigrade for the resistance method—namely to 45 degrees centigrade, 55 degrees centigrade, and 60 degrees centigrade. The test data show that, for any normal low-voltage induction motor, the resistance method is practically the equivalent of the thermometer method when thermometers are searchingly applied to locate the hottest observable part of the machine. The two methods agree within three degrees centigrade in most cases and seldom is the disagreement more than five degrees centigrade. The test results therefore indicate that an increase of not more than five degrees centigrade should be made in the present motor ratings to obtain the equivalent ratings by the resistance method. In fact, an increase of even five degrees centigrade must be justified partly on the grounds that more accurate results will be obtained by the resistance method than by thermometers as commonly used.

In actual practice over the past ten years, however, the points of measurement by the thermometer method have

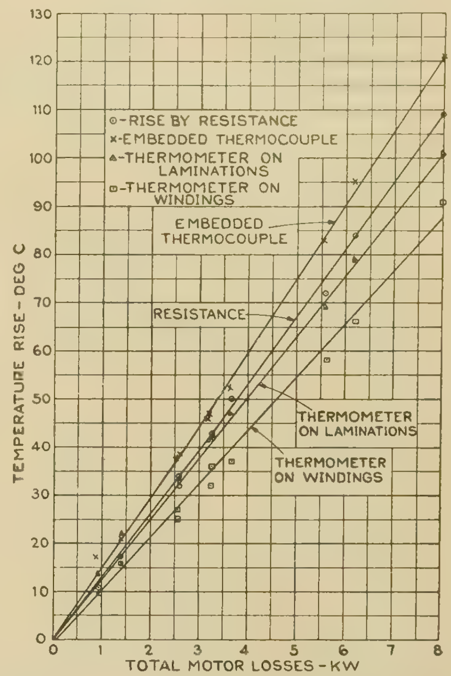


Figure 4. Relations between total losses and temperature rise by different methods of measurements for a typical 25-horsepower motor

(Refer to appendix V and table V)

not generally been the hottest surfaces. Hence, it is probable that 50-degrees-centigrade rise by resistance is a close approximation to the value that would be found by test on many partially closed motors now rated 40-degrees-centigrade rise by thermometer. The original 40-degree-centigrade rating was adopted in the standards on the assumption that thermometers gave values 15 degrees centigrade below the actual "hottest-spot" temperature; and when it is recognized that the resistance method gives test values that are approximately within 5 degrees centigrade below the hottest spot in many (but not all) motors, an argument is presented for adopting 50 degrees centigrade by resistance to replace the 40-degree-centigrade rating by thermometer.

To obtain confirming test data on the differentials between the internal "hottest-spot" and the limiting "observable" temperatures by different methods of measurement, thermocouples were distributed among the copper wires inside the stator slots when the random winding was installed in a typical low-voltage

Table I. Comparison of Two Totally Enclosed Fan-Cooled Motors of Different Manufacturers (See Appendix I and Figure 2)

Test Results for Motor X

	Front End				Pulley End			
*Position—inside coil periphery	1	2	3	4	10	11	12	13
Rise by thermocouple—degrees centigrade	54	54	54	53	58	58	58	59

	Front End				
*Position—outside coil periphery	5	6	7	8	9
Rise by thermocouple—degrees centigrade	27	41	39	30	27
Rise by thermometer—degrees centigrade	39	43	38	29	27

	Pulley End				
*Position—outside coil periphery	14	15	16	17	18
Rise by thermocouple—degrees centigrade	47	47	47	48	49
Rise by thermometer—degrees centigrade		46	46	48	48

Test Results for Motor Y

	Front End				Pulley End			
Inside coil periphery—degrees centigrade	50	50	51	51	49	51	51	50
Outside coil periphery—degrees centigrade	48	46	48	49	48	49	49	49

Temperature rise by resistance, motor X, one minute after shutdown 51 degrees centigrade.
Thermometers in positions 5 and 16 were not embedded in putty.
The other thermometers and all thermocouples were embedded in putty.
* Refer to figure 2 for locations of thermometers and thermocouples.
All above temperatures on motor Y were obtained with thermocouples.
Temperature rise by resistance, motor Y, 54 degrees centigrade.

25-horsepower motor as explained in appendix V. The heating tests on this motor, which are presented in table V and figure 4, show that for the three tests taken near normal load at 25.9, 26.4, and 25.8 horsepower, the corresponding differentials between the resistance method and the internal "hottest-spot" were six degrees centigrade, four degrees centigrade and five degrees centigrade, respectively—or an average differential of five degrees centigrade as compared to the present "conventional allowance" of ten degrees centigrade in paragraph 1–7 of AIEE Standards No. 1.

From the point of view of retaining the same limiting "hottest-spot" temperature of 105 degrees centigrade for class A insulation as originally intended in the standards, there is considerable justification for specifying 50 degrees centigrade by resistance in place of 40 degrees centigrade rise by thermometer in future standards. Such a step, however, might not leave sufficient margin for the conventional limits of voltage and frequency variation and for the 15 per cent service factor which are now specified in the ASA standards for general-purpose motors.

Similarly, 60 degrees centigrade and 65 degrees centigrade rise by resistance may ultimately replace 50 degrees centigrade and 55 degrees centigrade by thermometer for special-purpose and totally enclosed motors, respectively. However, a 65-degree-centigrade rating for class A insulation based on a 40 degrees centigrade ambient would not provide any margin for "hottest-spot" allowance. Actually the internal temperatures of an enclosed motor are normally more uniform than in an open-type machine. Nevertheless, it is logical that some margin for hot spots should be provided, because the resistance method can give only the average winding temperature. It may, therefore, be found desirable to accept 60 degrees centigrade instead of 65 degrees centigrade rise by resistance for totally enclosed motors, unless the standardized ambient temperature is reduced from 40 degrees centigrade to 35 degrees centigrade to be in closer agreement with prevailing temperatures in the United States. The long standing and justifiable precedent of rating open motors five degrees centigrade lower than enclosed machines may make it necessary to accept 55 degrees centigrade rise by resistance for special-purpose open motors, and in turn 45 degrees centigrade for general-purpose open motors, if 60 degrees centigrade by resistance is adopted as the temperature ceiling for totally enclosed construction.

Although the resistance method of temperature measurement is particularly adapted to inaccessible motors of partially or totally closed construction, test results demonstrate that it is also very satisfactory and dependable for open-type motors. However it may be desirable to retain the thermometer method in the standards for motors of entirely open construction, because it is the simplest possible procedure, and requires no special measuring equipment beyond the common mercury thermometer with which everyone is familiar. Even if resistance measurements are adopted as the standard method of testing and rating all types of motors, thermometers will still be needed for measurements of initial and ambient temperatures, bearing temperatures, for determining when the motor has reached a constant temperature, and for obtaining quick, approximate readings for check purposes.

Conclusions

To place motor specifications on a more sound basis, and to assure the motor buyer of more accurate and comparable temperature measurements, it is recommended that the resistance method of determining temperature rise be adopted as standard for all types of partially or totally enclosed induction motors, except for large, high-voltage machines for which the embedded-detector method should be retained.

It is also recommended that general industrial experience with respect to the present limits of temperature rise be reviewed, especially as regards "hottest-spot" allowances and differentials between various methods of measurement, so that the limits of temperature rise ultimately specified by the resistance method shall be in accord with the best interests of motor users.

The test data herein presented indicate that the new limits of rise by resistance should be at least five degrees centigrade above the present thermometer ratings for low-voltage machines with class A insulation. However, additional data and other considerations may indicate a ten-degree-centigrade increase to be logical, or may suggest separate temperature differentials for different voltage ratings or types of motor construction.

Appendix I. Comparison of Two Totally Enclosed Fan-Cooled Motors of Different Manufacturers

Comparative heating tests were made on two 75-horsepower totally enclosed fan-cooled motors having widely different construction features. These motors which are designated as X and Y were made by two different manufacturers. Both machines had the same NEMA frame number, speed, and horsepower rating, but motor X had a name-plate rating of 40 degrees centigrade rise whereas motor Y was rated 55 degrees centigrade.

Figure 2 shows the locations of 28 thermometers and thermocouples which were placed on the winding of motor X

through radial holes that were bored in the stator frame. One thermocouple was placed adjacent to each of the ten thermometer bulbs to obtain check readings. One of the thermometer bulbs on each end of the motor was not imbedded in putty in order to observe any difference in indication for this reason. Eight of the thermocouples were placed on inside periphery of stator coil ends directly over the rotor end rings. The values of temperature rise obtained at the different points for continuous operation at normal load are shown in table I.

By the thermometer method as defined in ASA standards 2.055 and 2.063, the temperature rise of motor X may be measured as anything from 27 degrees centigrade to 49 degrees centigrade depending on the number and location of points where readings are taken. The present standards do not specify the amount of exploration that shall be made to locate the hottest point accessible to thermometers. In this particular case the location and distribution of thermometers is of much greater significance than the details of how the bulbs are covered with putty or pads.

By placing thermocouples on the inside periphery of coil ends in positions not accessible to thermometers, higher values of temperature rise are obtained (53 degrees centigrade to 59 degrees centigrade).

The 51-degree-centigrade temperature rise of motor X by resistance method is only 2 degrees centigrade more than the maximum value of 49 degrees centigrade that could be obtained by following the ASA rules. If the testing personnel had used only two thermometers in positions 5 and 9, the temperature rise by the ASA rules would be measured as 27 degrees centigrade, or 22 degrees centigrade lower than the value of 49 degrees centigrade that might have been obtained.

On motor Y the temperature rise by resistance of 54 degrees centigrade is 5 degrees centigrade more than that obtained under the ASA rules, but is only 3 degrees centigrade above the values measured with thermocouples on inside coil periphery at points inaccessible to thermometers. The variation in temperature measurements by all methods on motor X is 32 degrees centigrade (59 degrees centigrade–27 degrees centigrade) whereas the corresponding variation on motor Y is only 6 degrees centigrade (54 degrees centigrade—48 degrees centigrade). The better uniformity of winding temperatures on motor Y is a result of a different type of winding and a more effective internal ventilation system. Consequently the thermometer method of ASA standards as usually applied would give results more nearly representative of maximum insulation temperature on motor Y than on motor X.

The tests on these two motors indicate that the temperature-rise-by-resistance method is much more dependable than thermometer readings which are obtained at a few points only. In this comparison the motor rated 40 degrees centigrade by thermometer actually had higher insulation temperatures than the 55 degrees centigrade rated machine. If the resistance method of test had been specified as standard, motor X definitely could not have qualified as a 40-degree-centigrade machine.

Appendix II. Multispeed Motors With More Than One Winding

If a motor has more than one stator-winding, surface temperature readings which are taken on the idle winding are of questionable value. It is common practice to place the winding with largest number of poles (and consequently the shortest end connections) inside next to the air gap. Even on open motors the underneath winding usually cannot be reached with thermometers, and it is utterly impossible to reach the inside winding of an enclosed machine by inserting thermometers in holes bored through the stator frame. Because of reduced ventilation at lower speed, the inaccessible winding is likely to be the limiting feature of the motor with respect to temperature rise on constant horsepower and constant torque ratings.

Table II shows thermocouple temperatures on both active and idle windings of three different four-speed motors having two stator windings. Machines M-1 and M-3 are of open-type construction, whereas M-2 is totally enclosed fan-cooled. Conditions of test including winding locations and temperatures of both active and idle windings are tabulated for nine different heating tests. Although a maximum difference of only 16 degrees centigrade appears on table II, differences up to 20 degrees centigrade have been observed.

Table II. Motors With Two Windings (Refer to Appendix II)

Symbol	Motor Rating
M-1	4/6/8/12 poles—60/40/30/20 horsepower—1,800/1,200/900/600 rpm—three phase—440 volts
M-2	4/6/8/12 poles—7.5/6.5/6/5 horsepower—1,800/1,200/900/600 rpm—three phase—440 volts
M-3	4/6/8/12 poles—50/33/25/17 horsepower—1,800/1,200/900/600 rpm—three phase—220 volts

	Conditions of Test			Temperature Rise—Degrees Centigrade	
Motor	Poles	Horsepower	Volts	Active Winding	Idle Winding
M-1	6	40	440	B...36	A...22
M-1	12	20	440	B...27	A...19
M-2	4	7.5	440	A...23	B...20
M-2	6	6.5	440	B...30	A...22
M-2	8	6	440	A...31	B...24
M-2	12	5	440	B...45	A...30
M-2	4	15	520	A...59	B...48
M-2	6	10	457	B...46	A...30
M-3	6	33	220	B...27	A...19

B—Indicates inaccessible underneath winding next to air gap.

A—Indicates outside winding farthest from air gap.

Two-winding wound rotors are not very common, but when such construction is used at least one of the windings is likely to be inaccessible on open motors, whereas both are inaccessible to thermometer method on totally enclosed motors as well as on most splashproof motors.

When the "sandwiched" type of coil is used on a two-winding stator, the inner winding is in middle of slot whereas the outer winding occupies both the top and bottom positions. This inner winding is quite inaccessible to either thermocouples or thermometers.

To avoid misleading data, the resistance method of temperature measurement is recommended for all multiwinding motors.

Appendix III. Analysis of Heating Data From Induction Motors Representing Various Types of Construction

In order to present a comprehensive analysis of the fidelity of the resistance method, a review was made of 264 heating tests made over a period of three years where temperature rises were measured by both the resistance and thermometer methods. The test data which are summarized in table III include a wide variety of motors representing different horsepower ratings, speeds, time ratings, electrical designs, and mechanical forms of construction. With the exception of a few of the wound-rotor measurements, each heating test was taken with a different motor. Approximately 70 per cent of the tests were made on motors in NEMA frame sizes from 364 to 505, inclusive. The remaining 30 per cent are for larger machines up to 1,000 horsepower. Several tests are averaged for each form of motor construction in order to minimize individual testing errors and thereby obtain a truly representative indication of results that could be anticipated by a general application of the resistance method.

All the surface temperature measurements on the stator windings of the totally enclosed and totally enclosed fan-cooled motors of table III were obtained with thermocouples which were well distributed in order to locate the hottest point. The testing procedure for the enclosed machines was carried beyond the requirements of the ASA standards, because some of the thermocouples were in locations that could not have been reached by inserting thermometers through holes in the frame. Consequently the recorded winding surface temperatures were approximately three degrees centigrade higher on the average than could have been obtained by the thermometer method.

The data on the open motors of table III were obtained by the thermometer method as defined in the present standards. In general, thermometers (or thermocouples)

Table III. Heating Data on Various Types of Induction Motors (See Appendix III)

Number Of Tests Averaged	Number of Motor Poles	Form of Mechanical Construction	Type of Motor	Temperature Measurements Taken on	§Average Length of Test in Hours	Temperature Rise by Thermometer (or Thermocouple) Degrees Centigrade: Winding	Temperature Rise by Thermometer (or Thermocouple) Degrees Centigrade: Laminations	Temperature Rise of Hottest Surface by Thermometer (or Thermocouple)—Degrees Centigrade	Temperature Rise by Resistance Method—Degrees Centigrade
17	2	♯Fan cooled	Squirrel cage	Stator	5.5	47.9	32.6	47.9	47.2
18	4	Fan cooled	Squirrel cage	Stator	6.0	51.5	35.3	51.5	50.2
15	6	Fan cooled	Squirrel cage	Stator	6.0	45.2	33.2	45.2	44.2
2	8	Fan cooled	Squirrel cage	Stator	7.4	52.5	36.0	52.5	49.5
4	10	Fan cooled	Squirrel cage	Stator	6.7	45.5	34.8	45.5	45.0
2	12	Fan cooled	Squirrel cage	Stator	6.0	46.5	33.0	46.5	45.5
3		Fan cooled	Squirrel cage	Stator	1.0	44.0	31.7	44.0	43.0
2		Fan cooled	Wound rotor	Stator	4.7	54.0	30.5	54.0	52.0
2		Fan cooled	Wound rotor	Stator	1.0	39.0	28.0	39.0	37.0
Total 65		Fan cooled		Stator		48.0	33.5	48.0	46.9† Average
3	4	*Totally enclosed	Squirrel cage	Stator	8.8	51.7	*40.7	51.7	52.0
3	6	Totally enclosed	Squirrel cage	Stator	7.8	41.3	*35.3	41.3	40.0
2	8	Totally enclosed	Squirrel cage	Stator	9.2	52.5	*41.0	52.5	53.5
2		Totally enclosed	Squirrel cage	Stator	0.5	36.5	*21.0	36.5	36.0
16		Totally enclosed	Wound rotor	Stator	0.56	35.7	*23.1	35.7	36.8
Total 26		Totally enclosed		Stator		39.5	27.8	39.5	40.1† Average
6	2	Splashproof	Squirrel cage	Stator	3.3	27.3	32.3	32.5	27.8
8	4	Splashproof	Squirrel cage	Stator	4.0	27.3	29.4	30.0	28.1
7	6	Splashproof	Squirrel cage	Stator	3.4	25.6	28.9	29.6	27.1
5	8	Splashproof	Squirrel cage	Stator	4.9	31.0	33.8	34.0	34.8
5	10	Splashproof	Squirrel cage	Stator	3.1	19.0	21.4	21.4	19.6
1	12	Splashproof	Squirrel cage	Stator	3.5	25.0	28.0	28.0	26.0
5		Splashproof	Squirrel cage	Stator	0.5	28.2	32.0	32.0	30.2
4		Splashproof	Wound rotor	Stator	5.4	36.0	38.3	38.8	39.5
Total 41		Splashproof		Stator		27.4	30.5	30.8	29.0† Average
25	2	Open	Squirrel cage	Stator	4.0	23.9	30.1	30.7	28.6
14	4	Open	Squirrel cage	Stator	3.7	21.9	26.9	26.9	22.7
8	6	Open	Squirrel cage	Stator	3.7	21.1	25.9	25.9	20.8
12	8	Open	Squirrel cage	Stator	3.9	26.3	28.8	30.7	27.5
8	10	Open	Squirrel cage	Stator	3.3	20.9	25.0	25.0	23.1
3	12	Open	Squirrel cage	Stator	3.0	17.3	21.0	21.0	20.0
2		Open	Squirrel cage	Stator	0.5	34.0	26.0	35.5	34.0
18		Open	Wound rotor	Stator	4.0	26.3	30.3	30.2	30.4
6		Open	Wound rotor	Stator	0.75	31.0	30.0	31.7	32.7
Total 96		Open		Stator		24.3	28.4	29.0	26.9† Average
16		*Totally enclosed	Wound rotor	Rotor	0.56	31.1	26.6	31.5	37.1
4		Splashproof	Wound rotor	Rotor	5.4	32.8	27.3	34.0	38.0
12		Open	Wound rotor	Rotor	4.0	24.0	23.1	25.2	28.9
4		Open	Wound rotor	Rotor	0.75	28.8	23.7	28.8	33.0
Total 36			Wound rotor	Rotor		28.7	25.2	29.4	34.0† Average

264—Total number of tests.

♯ Motors designated as fan-cooled are also totally enclosed.

* Motors designated as totally enclosed are not cooled by external fan. Stator lamination temperatures were measured on outside of frame on totally enclosed machines.

§All tests longer than one hour (a total of 208) were taken on continuous rated motors. The remaining 56 tests of one hour or less were taken on motors with short-time ratings.

†Averages of temperatures are weighted in accordance with number of tests taken for each condition.

were distributed over laminations and winding at ten or more points. All surface measurements on wound rotors were made with thermometers after shutdown.

Most of the data on splashproof machines were actually taken with thermocouples, but similar maximum values of temperature rise by surface measurements could have been obtained with thermometers if a sufficient number of holes had been bored in the motor frames. It will be noted that on both splashproof and open machines, the average temperature rise of laminations exceeded that of the accessible parts of windings. Consequently the hottest part of many motors will not be reached if thermometers are placed on the windings only.

Maximum observed temperatures are tabulated (regardless of whether the maximum value occurred before or after shutdown). In each case a conscientious attempt was made to find the hottest observable spots on the machine. The average deceleration period was approximately one-half minute, from time load was removed until motor was stopped. Resistance measurements were obtained from one to two minutes after motor stopped. (These time intervals were slightly greater for large, high-speed machines.)

The heating tests tabulated in table III were not all made at the normal rated loads of the various motors. Tests on machines of similar constructions but with different ratings (such as 40 degrees centigrade, 50 degrees centigrade, etc.) are averaged together. Consequently the average values of temperature rise shown in this table are not a direct indication of the margin between actual tests and the conventional name-plate ratings for the different classes of motors.

Appendix IV. Comparison of 12 Open-Type Motors of Different Manufacturers

Identical testing methods were used to determine the comparative temperature rises of 12 different low-voltage four-pole, 60-cycle motors. Eleven of these machines were general-purpose open-type 25-horsepower motors rated 40 degrees centigrade rise, but one motor (designated as K in table IV) was rated 50 degrees centigrade because it had some protective features. Each motor was made by a

different manufacturer. Complete test results for continuous operation at rated load are given in table IV.

The lamination and conductor temperatures were obtained by surface measurements with either thermometers or thermocouples depending in each case on the accessibility of motor parts. However, when thermocouples were used, they were purposely placed in locations that could have been reached by thermometers if holes had been bored through intervening structural parts. Therefore the data in table IV were obtained in accordance with rules 2.055 and 2.063 of the present ASA standards. Only the maximum observed temperatures are recorded, but measurements were made at many different points on each machine. With the exception of motor *I*, the resistance method does not deviate from the maximum temperature rise observed by surface measurements by more than 3.5 degrees centigrade, and on eight of the motors the agreement is within 2 degrees centigrade.

The tests on these 12 motors were made successively by the same personnel, and the same dynamometer and metering equipment were used throughout. Therefore most of those controversial points were eliminated which usually arise when an attempt is made to compare heating characteristics of different motors. Since the motors represent a wide range of design proportions and ventilation arrangements, the consistency of the temperature data obtained by the resistance method is highly significant.

Five of the 12 motors had a maximum observable temperature rise by thermometer that was higher than the name-plate rating. These tests indicate that comparable results are not being obtained by different manufactures under the present standards—presumably because of differences in the extent of exploration with thermometers.

Table IV. Comparison of Open Motors of Different Manufacturers (Refer to Appendix IV)

		Test Values of Temperature Rise—Degrees Centigrade			
Motor	Name Plate Rating—Degrees Centigrade	Laminations	Conductors	Maximum Rise by Surface Measurement	By Resistance
A	40	41	38	41	41
B	40	42	34	42	41
C	40	34	37	37	37
D	40	28	31	31	27.5
E	40	24.5	25.5	25.5	23.5
F	40	34.5	28.5	34.5	32
G	40	33	32	33	34
H	40	44	37	44	45
I	40	47	34	47	41
J	40	32.5	30.5	32.5	30.5
* *K*	50	59	61	61	62
L	40	29	24	29	27.5
Averages		37.4	34.4	38.1	36.8

* Motor *K* had some protective features.

Table V. Continuous Heat Runs for Various Loads and Voltages (See Appendix V and Figure 4)

(Motor Name-Plate Rating—Four Poles, 25 Horsepower, 220 Volts, Three Phase, 60 Cycles)

					Temperature Rise—Degrees Centigrade			
Voltage	Horsepower Load	Watts Input	Amperes	Total Motor Losses (Watts)	Winding Thermometer	Laminations Thermometer	Embedded Thermocouple	By Resistance
220	0	966	24.6	966	9	14	16	11
254	0	1,410	37	1,410	16	22	21	17
187	0	740	19.4	740	6	10	10	8
220	25.9	21,900	66.5	2,591	25	33	38	32
254	26.4	22,250	64.5	2,582	27	35	38	34
187	25.8	22,500	78.5	3,308	36	42	47	42
220	32.3	27,800	83	3,729	37	47	53	50
254	32	27,200	76	3,285	32	42	46	43
187	31.5	29,700	107	6,190	66	79	95	84
220	38.0	34,000	103	5,636	58	69	83	72
220	41.9	39,200	120	7,976	91	101	121	108

Appendix V. Temperature Measurements for Various Conditions of Motor Load and Voltage

An open-type 25-horsepower motor rated 220 volts at 1,800 rpm was selected for a series of 11 heat runs covering a wide range of conditions from zero to 168 per cent of normal load and from 85 to 115 per cent of rated voltage. Temperatures were measured by thermocouples which were inserted in internal parts of stator slots when the random winding was installed, by thermometers on windings and laminations, and by resistance method as tabulated in table V for continuous heat runs at each condition indicated. The laminations and windings were readily accessible, and thermometer readings were taken according to the ASA rules.

For operation at rated voltage, the resistance method indicated one degree centigrade to three degrees centigrade lower temperature than thermometers at normal load or less, but for extreme overloads the temperature is higher by resistance, the maximum difference being seven degrees centigrade at 168 per cent of rated load.

Thermocouples were distributed through the stator slots in order to locate the hottest internal point in the motor, and the maximum temperatures observed are designated as "embedded thermocouple" in table V and figure 4. In this random winding the embedded thermocouples are in close contact with the conductors, and are not separated from them by coil insulation as in the case where embedded-detectors are placed between two formed coils in the slot of a high-voltage motor. Therefore these embedded thermocouples indicate closely the theoretical "hottest spot" of the winding which was located at the middle of lamination stack. The "hottest spot" averaged five degrees centigrade higher than the temperature by resistance for the heat runs taken near normal load.

Figure 4 shows that the temperature rises by all methods are approximately proportional to the total motor losses. In general, increasing differentials are obtained between the temperature measurements by different methods as the motor losses are increased by overloading the machine.

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Discussion

For discussion, see page 472.

Measurement of Temperature in General-Purpose Squirrel-Cage Induction Motors

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FELLOW AIEE

THERE are three fundamental methods of temperature determination[1] which might be applied to squirrel-cage motors, namely:

1. The thermometer method
2. The resistance method
3. The embedded-detector method

It is the purpose of this paper to report results obtained by the three methods and to discuss the practical aspects of temperature measurement.

The standards of the American Standards Association,[2] AIEE,[3] and National Electrical Manufacturers Association[4] specify that the temperature rise of induction machines shall be determined by the thermometer method. Section 50 of the Test Code for Polyphase Induction Machines[5] is more general and reads as follows:

> Temperature tests are taken primarily to determine the amount of temperature rise on the different parts of the machine while running under a specified load. This rise in temperature is measured by either the rise in resistance of the current-carrying part, or by means of a thermometer, thermocouple, or embedded temperature detector. It is sometimes desirable to use one method as a check on the other.
>
> In measuring temperature rise by resistance, care must be taken to observe the precautions set forth in paragraphs 10 to 16 in order to secure accurate results, since a small error in measuring resistance may cause a comparatively large error in determining the temperature.
>
> The usual method of measuring temperature of open machines is by the use of alcohol or mercury thermometers or thermocouples, applied to the hottest part of the motor that is accessible. In the case of totally enclosed machines, due to the mechanical difficulties attending the use of alcohol or mercury thermometers, both during the run and after shutdown, thermocouples may be more convenient. . .

The test code specifies that thermometers or thermocouples shall be applied to the hottest part of the motor that is accessible. Definition 2.055 of the American Standards for Rotating Electrical Machinery[2] is more specific and reads as follows:

> *Thermometer Method of Temperature Determination Defined.* This method consists in the determination of the temperature, by mercury or alcohol thermometers, by resistance thermometers, or by thermocouples, any of these instruments being applied to the hottest part of the machine *accessible to mercury or alcohol thermometers.*

Thermometers Versus Thermocouples

It would seem that if proper precautions are observed in placing the instruments, the same results should be obtained with either thermometers or thermocouples. In order to check this assumption, several hundred temperature test records of squirrel-cage motors in sizes from 1 to 100 horsepower were examined. The temperature tests made before 1936 employed thermometers, and the results were checked by the resistance method. Tests made during the last two years employed thermocouples and the results were again checked by resistance measurements. It therefore seemed practical to obtain a comparison between thermometers and thermocouples by using the resistance measurements as a reference. The results of this comparison were disappointing. It was possible to arrive at the general conclusion that the temperature rise measured by thermocouple is higher than that measured by thermometer, but there was too much variation to justify establishing a differential which could be used for all conditions.

There are several explanations for the discrepancies. In the first place, the resistance measurements were only used as a check and it is likely that not enough emphasis was placed on the precautions which must be observed when using this method. The instruments were commercially accurate and readings were taken carefully, but there was probably not enough attention paid to the following paragraph (11) of the test code:[5]

> Every possible precaution should be taken to obtain the true temperature of the winding when measuring the cold resistance. The temperature of the surrounding air must not be regarded as the temperature of the windings unless the motor has been standing idle under constant temperature conditions for a considerable period of time.

It was also thought that some of the discrepancies in temperature tests might be due to inherent differences between thermometers and thermocouples.

Thermometers are calibrated while immersed in liquid to a specified depth, and it goes without saying that the liquid makes intimate contact with all parts of the bulb and part of the stem. Furthermore, thermometers are inflexible and

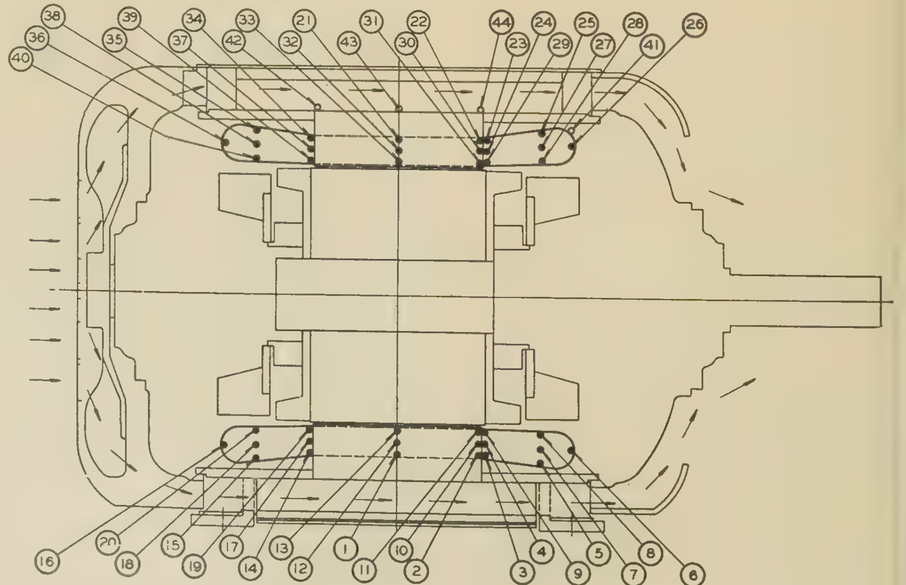

Figure 1. Location of thermocouples in totally enclosed fan-cooled motor

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Paper number 39-5, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 31, 1938; made available for preprinting November 28, 1938.

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1. For all numbered references, see list at end of paper.

due to their shape, can touch a plane surface, or most curved surfaces, at only two points. The question might therefore be raised whether a thermometer gives a true indication of the temperature of a winding, even when the bulb is covered with putty. On the other hand, a thermocouple is made of small, flexible wires and the junction can be put in a small opening and can be made to have intimate contact with a winding. Check tests made on cylindrical coils showed that thermocouples read higher than thermometers, the difference depending upon the curvature and smoothness of the coil surface, and the care used in making the application.

When a thermometer is used for measuring the temperature of a winding, at least a part of its stem is usually exposed to the cooling air and it was thought that this might affect the readings. This was checked by immersing a thermometer bulb in boiling water and directing the air from a desk fan on the entire stem. It was found that the reading was not affected as long as any appreciable amount of the bulb was immersed. With only the tip of the bulb in the water, the application of the fan reduced the reading three per cent.

In order further to investigate the subject, it was decided to build two 40-horsepower motors and to make careful temperature tests on these motors, by resistance, thermocouple, and thermometer, and to place enough thermocouples in the windings to locate surely, and measure the temperature of the hot spot. It was decided to build one motor of the totally enclosed fan-cooled type, and to test this motor with two rotors, one with heavy load losses and the other with relatively small load losses. It was decided to make the other motor an open-type machine and to test this motor with one rotor having small load losses.

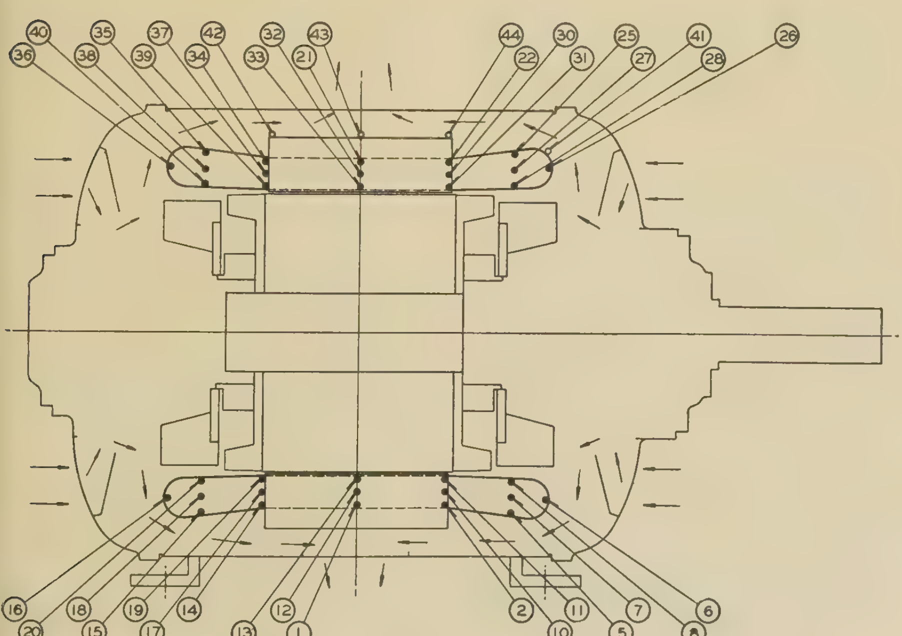

Figure 2. Location of thermocouples in open-type motor

Location of Hot Spot

Careful consideration was given to the location of the hot-spot thermocouples and useful information was obtained from a series of papers[6–11] presented at the midwinter convention of 1913. From these papers the conclusion was reached that the location and magnitude of the hot spot in the stator winding of a squirrel-cage motor depend upon its

1. Physical characteristics
2. Electrical and magnetic characteristics
3. Ventilation
4. Loading

A motor which is symmetrical about its vertical center line usually has a blower on each side and both free ends of the stator winding should be equally cooled. The parts of the coils which are embedded in the core are not so well ventilated and it seems likely that the highest coil temperature will be reached in the axial center of the core. If the design is unsymmetrical especially in case there is a blower on only one side of the motor, the free ends of the winding which are on the same side as the blower will be cooler than those on the opposite side, and the hot spot will probably shift away from the blower. The location of the hot spot vertically in the slot is not so easy to estimate, and will depend to a great extent on the direction of the heat flow between the winding and core. The direction of heat flow will depend on which member is producing the greater amount of heat, which, for a given load, will vary with the electrical design, and for a given design will vary with the loading. With light loads the copper losses will be small and heat may flow from the core iron to the winding. At a certain heavier load there may be little interchange of heat between the core iron and the winding, and at still heavier loads, the heat may flow from the winding to the core. The winding near the mouth of the slot will not be as closely associated with the core iron as the rest of the winding, but on the other hand, will be subjected to the heat produced in the rotor, which, in a poorly designed motor, may be excessive.

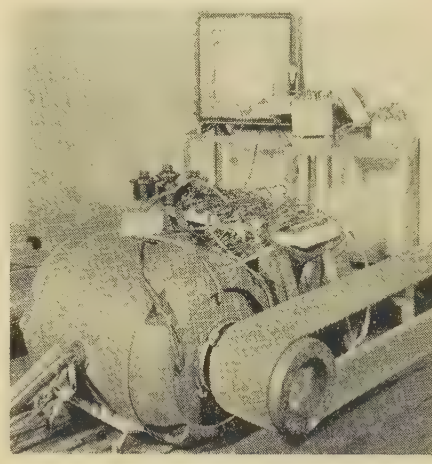

Figure 3. Equipment used for testing totally enclosed fan-cooled motor

In order to be reasonably sure of finding the hot spot, 44 thermocouples are installed in the totally enclosed fan-cooled motor as shown in figure 1. A thermometer extends through a slot in the end plate and the bulb of the thermometer is immediately adjacent to thermocouple number 41. There are 38 thermocouples in the open motor as shown in figure 2, and again the bulb of the thermometer is immediately adjacent to thermocouple number 41. Thermocouple number 41 and the thermometer bulb are on the outside of the tape which covers the free ends of the windings and are covered with a small amount of sealing compound. Thermocouples number 42, number 43, and number 44 are located on the stator iron and are also covered with a small amount of sealing compound.

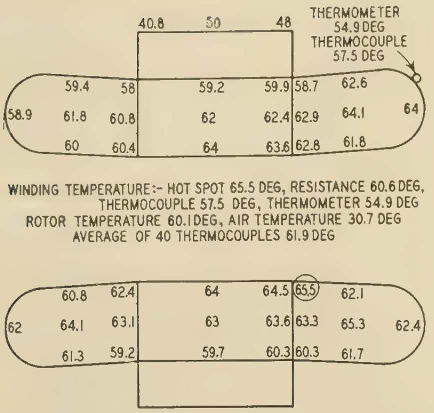

Figure 4. Totally enclosed fan-cooled motor at 75 per cent full load

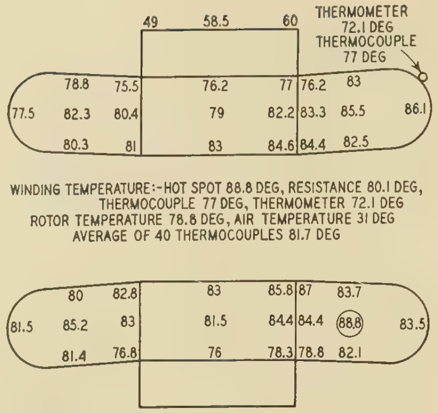

Figure 5. Totally enclosed fan-cooled motor at full load

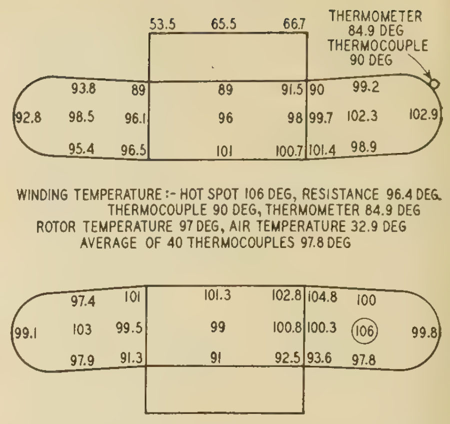

Figure 6. Totally enclosed fan-cooled motor at 115 per cent full load

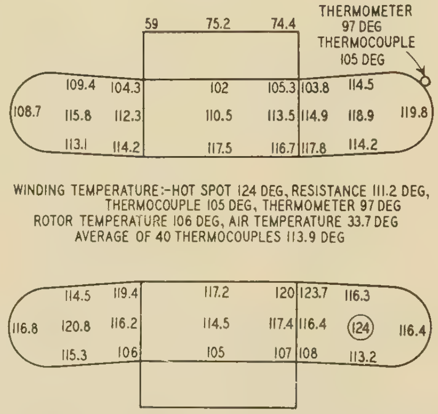

Figure 7. Totally enclosed fan-cooled motor at 125 per cent full load

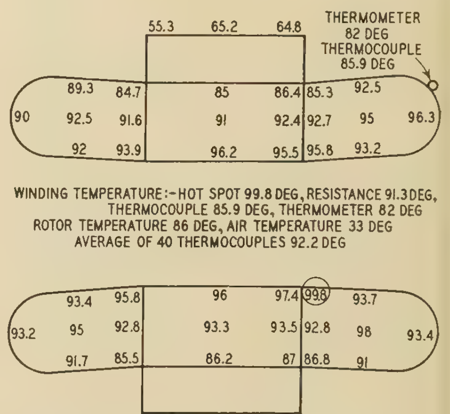

Figure 8. Totally enclosed fan-cooled motor at full load (heavy load losses)

Thermocouples numbers 11, 13, 31 and 33 are silk taped to the top of a top coil side, next to the 0.025-inch insulation separating the winding from the one-eighth inch wooden wedge. Thermocouples numbers 10, 12, 30 and 32 are silk taped to the top of a bottom coil side, just below the 0.060-inch insulation separating the coil sides. Thermocouples numbers 1, 2, 21, and 22 are silk taped to the bottom of a bottom coil side, next to the 0.025-inch slot cell. (A top coil side is the one nearer the air gap; a bottom coil side is the one nearer the outside of the motor.) Thermocouples numbers 8, 9, 19, 20, 28, 29, 39, and 40 have radial positions corresponding to number 11, etc., thermocouples numbers, 4, 7, 17, 18, 24, 27, 37, and 38 have radial positions corresponding to number 4, etc., and thermocouples numbers 3, 5, 14, 15, 23, 25, 34, and 35 have radial positions corresponding to number 1, etc., but are on the free ends of the windings just inside the tape which covers the free ends. Thermocouples numbers 6, 16, 26, and 36 are silk taped to the loops of the coils, just inside the tape. Figures 1 and 2 are drawn approximately to scale and show the relative dimensions of the various parts as well as the location of the thermocouples. Both stators are wound to the same specification, of double-cotton-covered wire, with double-layer diamond windings in semienclosed slots. The stators have only one dipped and baked coat of varnish, additional coats having been omitted to save time. There are no ventilating ducts in either the stator or rotor and no openings through the rotor. The squirrel cages are of cast aluminum construction.

Apparatus and Test Results

The arrangement of the apparatus is shown in figure 3. The thermocouple leads are enclosed in spaghetti tubing and are divided into four groups and brought out of the motor through the air-gap plug holes. The leads are connected to terminal boards and may be connected either to an indicating or recording instrument by means of dial switches. A double bridge was used to measure the resistance of the stator winding, and the temperature of the rotor was measured by means of a thermocouple attached to the tip of an air-gap gauge and inserted through one of the air-gap holes. At the end of each run, the rotor was stopped very quickly, the stator winding was disconnected from the line, the stator resistance was measured in a minute or less, and the rotor temperature was measured in two minutes or less.

The results of the tests on the totally enclosed fan-cooled motor are shown graphically in figures 4 to 9, inclusive. Figures 4, 5, 6, and 7 show the temperatures in the various parts of the motor after becoming constant, at 75, 100, 115, and 125 per cent load respectively, with a standard rotor. Figure 8 shows the temperatures attained at 100 per cent load with a rotor having heavy load losses. The figures in the large circles are the hot spots. Figure 9 shows the relation between hot-spot temperatures and temperatures by resistance, thermocouples, and thermometers for the various tests illustrated in figures 4, 5, 6, and 7. It is believed that the diagrams and curves

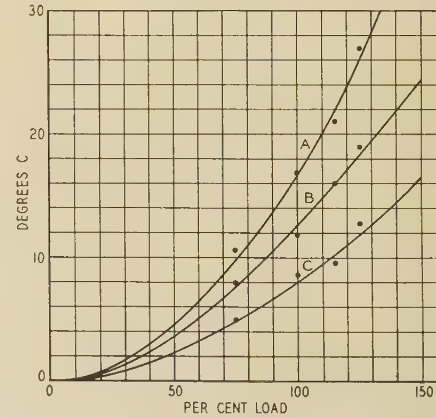

Figure 9. Totally enclosed fan-cooled motor

Forty horsepower, three phase, 60 cycles, 220/440 volts, 1,750 rpm

A—Hot-spot temperature minus temperature by thermometer

B—Hot-spot temperature minus temperature by thermocouple

C—Hot-spot temperature minus temperature by resistance

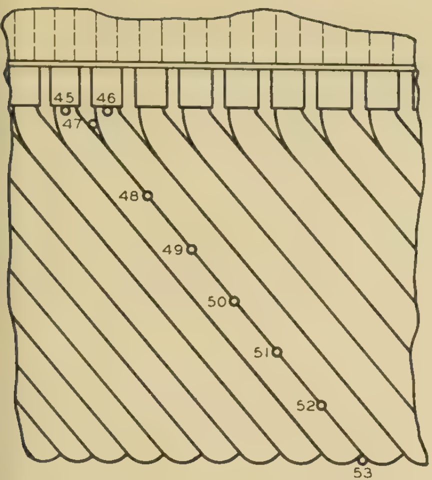

Figure 10. Temperature in coil extension of open-type 40-horsepower motor at full load

Nine Thermocouples

Number	45	46	47	48	49	50	51	52	53
Deg C	46.6	44.3	44.3	53.0	53.1	54.1	54.1	42.8	44.1

are self-explanatory, and the test results seem to be consistent with each other. It should be noted that the cooling fan is on the left side and the hot spot is on the right side of the fan-cooled motors. One point of particular interest is the close agreement between the average of the temperatures obtained from thermocouples numbers 1–40, inclusive, and the temperature by resistance, indicating that the thermocouples are located judiciously, and that the readings are accurate.

Similar tests were made on the 40-horsepower open-type motor and the results were not what had been anticipated. The open-type motor is supposedly symmetrical about both center lines and it is reasonable to expect that the temperatures of corresponding parts would be identical. However, the coil extensions on the pulley end are considerably hotter than on the opposite end. An examination of the motor showed that the windings are not symmetrical but are considerably shorter on the pulley end, which probably accounts for the difference in temperature. The readings of the thermometer and thermocouple number 41 were much lower than expected and very much lower than the temperature by resistance and hot-spot temperature. This made it seem probable that they were not located in the right place and the motor was inspected with this idea in mind. It was found that the thermometer was inserted through a hole drilled in the frame with the thermocouple immediately adjacent to the thermometer bulb, and while the location of the thermometer was not ideal, it did not seem to be far away from the proper location. In order to find the hottest external part of the coil extension, nine more thermocouples were placed on the surface of the winding as shown in figure 10. It will be noted that thermocouples number 50 and 51 give results about ten degrees higher than number 52 and 53. Temperature tests were then made at 100 per cent, 125 per cent, 144 per cent, and 160 per cent of full load using thermocouple number 51 and a thermometer whose bulb is immediately over this thermocouple. The results of these tests are shown graphically in figures 11 to 15 inclusive and are self-explanatory. The averages of thermocouples number 1–40 inclusive do not check the temperature by resistance, but fair agreement is obtained by eliminating numbers 6, 8, 16, 20, 26, 28, 36, and 40, which are located directly in the path of the cooling air. Figure 15 shows the relation between hot-spot temperatures and temperatures by resistance, thermocouples, and thermometers for the various tests illustrated in figures 11–14 inclusive.

Conclusions

A revision of the standards which specify the method of measuring the temperature rise of induction machines is desirable and should be undertaken. The determination of temperature rise should not depend on surface measurements taken either by thermometer or thermocouple, because it is often difficult, and sometimes impossible to locate these instruments properly. Even though a machine is so constructed that there is no interference with the installation of thermometers, it is not good practice to leave the location of thermometers to the judgment of even the most careful and conscientious testers, because they ordinarily have no way of determining the hottest accessible spot. Elimination of thermometers and thermocouples will leave only the resistance method for machines of ordinary size and voltage, and while this method requires skillful technique on the part of the tester, it has been used in transformer heat runs for many years and is strictly a commercial method.

If the resistance method is adopted, it will be necessary to revise the conventional allowances[1] for hot spot and to increase the standard temperature ratings for induction machines about five degrees

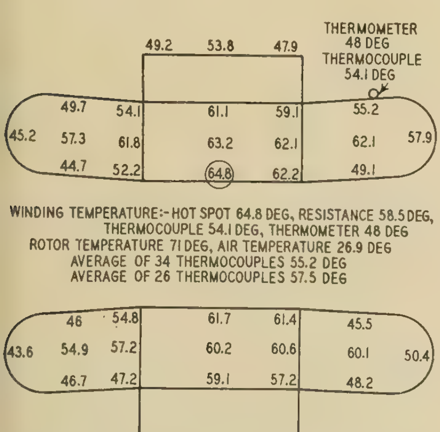

Figure 11. Open-type motor at full load

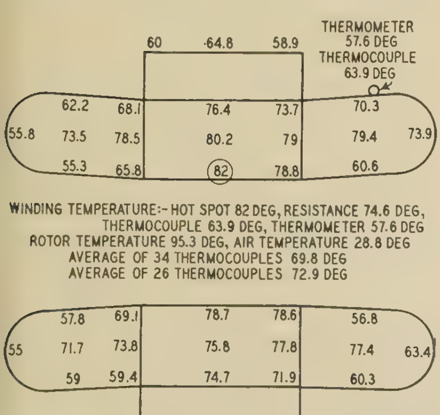

Figure 12. Open-type motor at 125 per cent full load

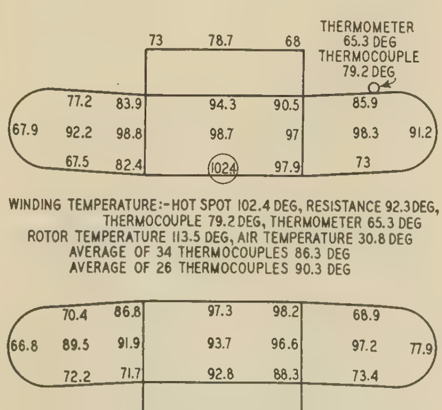

Figure 13. Open-type motor at 144 per cent full load

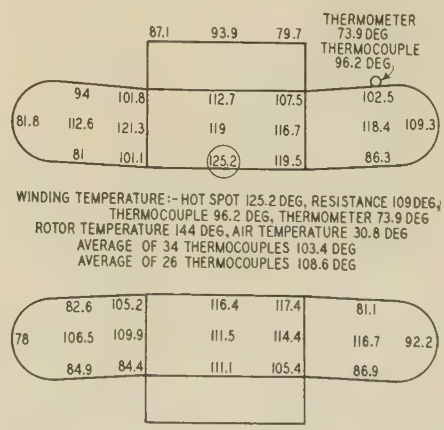

Figure 14. Open-type motor at 160 per cent full load

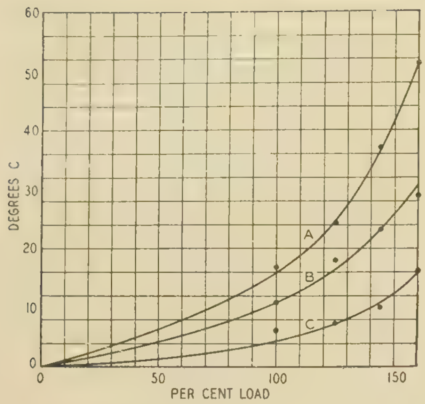

Figure 15. Open-type motor

Forty horsepower, three phase, 60 cycles, 220/440 volts, 1,750 rpm

A—Hot-spot temperature minus temperature by thermometer

B—Hot-spot temperature minus temperature by thermocouple

C—Hot-spot temperature minus temperature by resistance

centigrade. Such an increase would result in the following ratings:

Open-type continuous ratings 45 degrees centigrade

Protected and intermittent ratings 55 degrees centigrade

Totally enclosed continuous ratings 60 degrees centigrade

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4. NEMA Motor and Generator Standards, publication number 38–49, May 1938, page 35, note III; page 51, note II; page 58, note II.

5. Test Code for Polyphase Induction Machines, AIEE Standards No. 500, August 1937, pages 12–14.

6. High Speed Turboalternators—Designs and Limitations, B. G. Lamme. AIEE Transactions, 1913, pages 19–25.

7. Temperature and Electrical Insulation, C. P. Steinmetz and B. G. Lamme. AIEE Transactions, 1913, pages 83–9.

8. Notes on Internal Heating of Stator Coils, R. B. Williamson. AIEE Transactions, 1913, pages 153–62.

9. Measurement of Temperature in Rotating Electric Machines, L. W. Chubb, E. I. Chute, and O. W. A. Oelting. AIEE Transactions, 1913, pages 163–75.

10. Method of Determining Temperature of Alternating Current Generators and Motors and Room Temperature, Henry G. Reist and T. S. Eden. AIEE Transactions, 1913, pages 177–84.

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Discussion

P. H. Rutherford (General Motors Corporation, Dayton, Ohio): The results shown in the paper by C. P. Potter agree very well with the results of similar tests carried out on small single-phase motors. Extensive tests carried out on a one-horsepower single-phase motor showed that the hot spot occurred in the center of the end turns of the winding and did not appear to shift with variations in load. This hot-spot temperature was approximately ten per cent greater than the temperature rise by resistance, a result which checks quite well with data obtained from much larger motors. If the resistance method proves satisfactory, it might be well to consider the allowances for hot spot as a per cent of the temperature rise by resistance rather than a constant allowance of about 5 degrees centigrade. This would serve to extend temperature standards to motors of ratings higher than 60 degrees centigrade in case such designs should prove desirable in the future.

It would have been interesting to consider the curves in this paper plotted against motor losses as well as per cent of full load. Since one motor was tested with a standard rotor and a rotor with heavy load losses, it would seem that for the same losses the temperature rise and possibly the hot-spot temperatures would agree quite well. A comparison of the temperature versus losses curves for the open and the totally enclosed type motors would also be very interesting.

P. L. Alger (General Electric Company, Schenectady, N. Y.): The discussion has brought out a possible difficulty in obtaining the correct temperature rise of a motor by the resistance method, due to the time delay between the instant of shutdown and the time the measurement is taken. This same problem occurs in other types of apparatus, for which the resistance method of measurement is generally used. I should, therefore, like to call attention to paragraph 10.292 of appendix I of the proposed ASA standards for transformers, which gives a simplified method of determining this correction by calculation. This method is to apply an empirical correction to the degrees centigrade as measured by the resistance method. The correction is equal to the product of the watts loss per pound of copper in the winding under test (determined by the current density in the copper) multiplied by a factor that depends upon the time elapsed between the instant of shutdown and the time the measurement is taken, as given in the following table:

Time in Minutes	Factor
1	0.19
$1^1/_2$	0.26
2	0.32
3	0.43
4	0.50

The factors given in this table apply to oil-insulated windings, and are representative of the normal rate of heat flow from the copper into the oil. It seems clear, however, that similar factors can be derived for motor windings or other apparatus, that would be quite accurate enough for test purposes, in view of the small magnitude of the correction.

For example, with a current density of 3,000 amperes per square inch in the copper, and a temperature of 75 degrees centigrade, a time delay of two minutes corresponds to a correction of only 2.4 degrees centigrade.

G. R. Anderson (Fairbanks, Morse and Company, Beloit, Wis.): Any proposal for revision of existing standards naturally brings out data and experience that should be utilized to the best advantage in its consideration.

The various papers presented have outlined fully the many factors that must be considered, the variations to be expected, and the weight to be given to each.

Referring to the recommendations in C. P. Potter's paper, the writer has taken the opportunity of making an analysis of approximately 100 tests on various sizes of enclosed fan-cooled motors ranging from 1 to 100 horsepower. The list selected contained only ratings designed for general-purpose application. Those with special characteristics or having large rotor load losses were eliminated. Each motor had been tested for shutdown winding temperatures by thermometer and by resistance. Averaging these tests gave the following:

Average increase of temperature rise by resistance measurement over thermometer measurement was 6.9 degrees centigrade.

Average increase in percentage was 19.2.

Another analysis of the same tests was made by selecting only 40 ratings that had temperature rise by either resistance or thermometer between the range of 40 degrees centigrade and 60 degrees centigrade. The averages of this group gave the following:

Average increase of temperature rise by resistance measurement over thermometer measurement was 8.2 degrees centigrade.

Average increase in percentage was 17.4.

It will be noted that these percentages compare favorably with those recorded by Mr. Potter, which averaged 22 per cent on the enclosed fan-cooled motor tested by him.

On the basis of these data it would appear reasonable to recommend that where present standards by thermometer measurement are now 40 degrees rise, that permissible equivalent resistance measurements should fall somewhere in the neighborhood of 47 and 48 degrees centigrade and that where maximum permissible thermometer temperature measurements are 55 degrees centigrade, the resistance method should allow 65 degrees centigrade.

In addition to the above it is recommended

1. That the present 40-degree-centigrade standard by thermometer measurement be retained for all machines having modifications to which this type of measurement can be applied.

2. That resistance measurement be permitted for enclosed machines and those having mechanical modifications which make the application of thermometers impractical.

3. That the resistance measurement be the only method adopted as standard for field-coil temperature measurement.

. G. Veinott (Westinghouse Electric and Ianufacturing Company, Lima, Ohio): n the course of the application of thermal rotective devices the writer had occasion o take numerous temperature runs on a ne of fractional-horsepower motors at high egrees of overload, causing high tempera- ure rises. Temperatures were measured y a thermometer placed on the "hottest ccessible" part of the end winding, by a hermocouple installed on the end winding *not an embedded detector*), and by rise of esistance. The results are tabulated in able I of this discussion.

Table I

Iorse- oower Rat- ing	Per Cent Load	Open or Enclosed	Shutdown Temperature Rises by: Ther- mome- ter	Ther- mo- couple	Re- sist- ance
1/6	200	Open	61	83	77.5
1/6	160	Enclosed	76	89	79.2
1/4	190	Open	66	82	83.3
1/4	140	Enclosed	78	88.5	83.5
1/3	180	Open	66	86	85.4
1/3	130	Enclosed	70.5	89	74.5
1/2	180	Open	58.5	79.5	71.5
1/2	130	Enclosed	75.8	84.8	82.7
3/4	160	Open	87	100	100
3/4	130	Enclosed	89	97	81.5

As a result of these and other tests, the vriter draws the following conclusions as pplying to fractional-horsepower motors:

. Thermocouples should be recognized form of emperature measurement. It is frequently the ractice of builders of motor-driven appliances to un application tests on their machines. In many ases, thermocouples are practically the only way f measuring the temperatures attained by the vindings because the motor itself is not readily ccessible and sometimes control circuits are so nterlocked that it is not easy or even possible to neasure the rise by resistance. Refrigeration and ir-conditioning appliance manufacturers neces- arily use thermocouples for the temperature meas- rements made to determine the thermodynamic erformance of the whole apparatus; thus thermo- ouples afford the simplest and easiest means for neasuring the motor temperature.

. The limiting temperatures, when measured by thermocouple installed on the "hottest accessible pot of the windings," should be the same as the imiting temperatures for the rise-of-resistance nethod, instead of the same as by thermometer s now. This recommendation is conservative ecause the thermocouple temperatures are enerally higher than those obtained by rise of esistance.

. The rise-of-resistance method is not reliable or measuring the temperature of an auxiliary wind- ng if there is a starting switch in the circuit be- ause of variations in contact resistance. Very erious errors have been observed because of switch ontact resistance.

. In view of the fact that, with thermal overload evices, some very high temperature rises are often neasured, it might be better to express the correc- ion for the different methods of temperature rise s a per cent of the measured rise, instead of using flat correction in degrees as now. Potter's gures 9 and 15 support this conclusion.

. S. Hill (University of Maine, Orono): Ay experience with the thermometer nethod in research work on fully enclosed an-cooled induction motors supports Mr. Summers' conclusion that reliable winding emperature data cannot in general be ob- ained on such machines by inserting ther- nometers through holes in frames or covers at the close of heat runs in accordance with ASA rule 2.063. In my first series of experiments on motor ventilation, back in 1932, an attempt was made to determine ultimate insulation temperatures in this way; but, despite the fact that the thermometers were always applied by the same personnel under conditions presumably more conducive to accuracy than those usually encountered in field tests, results were so inconsistent that the procedure was soon abandoned in favor of resistance measurements.

The impossibility of properly covering bulbs with pads or putty when, on shutdown, thermometers must be quickly applied through small openings, is in itself a sufficient justification for a revision of the standards relating to temperature measurements on partially enclosed and totally enclosed machines. Thermometer observations of stator core temperatures, in the fan-cooled motor investigation just mentioned, indicate that even when the bulbs are immersed in oil in small wells drilled in the core structure to receive them, and are as a consequence afforded a fairly close contact with the laminations, and almost complete protection from the influence of air streams, the readings are likely to be from four to eight degrees too low unless the annular openings around the stems at the top of the bulbs are completely closed with cones of putty. In commercial testing under existing standards, where thermometers have to be pushed in through holes at the conclusion of a run, the "point contact," justly criticized by Mr. Summers, is about all that can be expected; and, although at standstill the bulbs are not subject to cooling by forced convection, the large percentage of bulb area exposed to the surrounding air is certain to have a marked but indeterminable effect in lowering the reading. Such observations, though possibly of interest in comparing the performance of different designs, are obviously inadequate as a basis for rating machinery.

Both Mr. Potter and Mr. Summers very properly emphasize the great importance of an accurate determination of the winding temperature corresponding to "cold resistance." This correlation is unquestionably the most vital step in the successful application of the resistance method as a means of securing dependable heating data on machinery insulation, particularly in the case of motors of the enclosed type. For as the extent of enclosure is increased, not only are the windings less accessible to thermometers, but much more sluggish in cooling after load tests or in following changes in ambient temperature. With a completely closed structure many hours may elapse after any thermal disturbance before the copper reaches the exact temperature of parts, such as laminations, bearing brackets, or shields, to which thermometers can be conveniently applied. In an endeavor to attain an accuracy well within one degree in the evaluation of average winding temperatures, I have found it necessary to remove both end covers of the motor enclosure, place thermometers on the insulation and adjacent laminations, and, taking readings at regular intervals, defer the "cold resistance" measurement until a state of complete thermal equilibrium with the ambient was observed. While such a degree of disassembling would doubtless be inconvenient in factory testing, and ordinarily impracticable in the field, its desirability in an engineering investigation shows that the code requirements quoted by Mr. Potter and the precautions urged by Mr. Summers are extremely important and none too severe.

In an extended research where, after due investigation, thermometers can be strategically and permanently placed with bulbs properly protected from the influence of surrounding air, the temperatures which they indicate may be fully as consistent as those determined by any other means; but when hurriedly applied after shutdown, with insufficient contact area, inadequate bulb protection, and no certainty as to correct location, the accuracy of the resulting hottest-spot temperature cannot compare with that attainable by resistance measurements. Moreover, in loading-back tests, the resistance method offers the possibility of checking winding temperature at any time during the run by the momentary removal of a-c power to permit bridge observations, the machine being driven by its loading generator during this interval. Whether or not resistance values obtained under these conditions would be acceptable as a criterion of ultimate insulation temperature at the end of test, is open to question; but, in some instances at least, I have found them to yield results apparently more dependable than those of readings taken after the machine was shut down.

L. A. Kilgore (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): In the measurement of temperature by resistance, there is an additional difficulty not mentioned by C. P. Potter and E. R. Summers. This difficulty arises from the fact that the temperature of a winding changes very rapidly in the first few minutes after shutdown and the reading at one minute after shutdown may be about two to six degrees lower than the actual average temperature; furthermore, if one tester gets the readings in 1/2 minute and another in 1 1/2 minutes, the readings may differ by about this same amount.

There is an accurate method of overcoming this difficulty which the writer believes should be incorporated as part of any proposed standard test by resistance, to be used whenever the readings cannot be taken within one-half minute. This method consists of plotting the curve of temperature (or resistance) against time and projecting back to the instant of shutdown. The initial slope of this time-temperature curve can be shown to be the initial average loss per unit of conducting material divided by the thermal capacity. Thus, in copper (neglecting eddy currents and using constants for copper at 75 degrees), the initial rate of change in degrees centigrade per minute is equal to 0.9 times (current density in thousands of amperes per square inch).[2]

With tests made in this more accurate manner (projecting back to the instant of dumping load), our general experience on open motors above 200 horsepower has been that 10 to 15 degrees differential exists between temperature by resistance and by thermometer. For totally enclosed motors, this differential is somewhat less, depending on how the machine is ventilated

and how hard it is worked, but tests on a number of motors indicate a five- to ten-degree differential.

It may be desirable to use temperature by resistance for enclosed machines and other inaccessible machines, but before this can be made standard, an agreement must be reached on the exact method of test and on the proper differential between resistance and thermometer temperature limits.

Henry Thomas (Sun Oil Company, Philadelphia, Pa.): The papers by Mr. Summers and Mr. Potter have covered the subject so well and presented data and logical discussions in such a complete form, that there is little to be added. The points are brought out so clearly showing the weakness of the existing methods of temperature determination and the advantages and the reasonableness of the resistance method, that there seems no answer except to adopt it as the only consistent, accurate, and convenient method to be used for all types of motors, but especially that of the enclosed, or partially enclosed type.

The authors have brought out very clearly that the thermometer method at best gives quite variable results, cannot be checked by any two persons, even those experienced, and that with a very large number of present-day motors it is impossible, or practically so, to use thermometers.

I have very strongly favored the use of the resistance method for use on induction motors since 1930 and this method has been specified on all equipment purchased by the company that I represent since that time. A great many tests have been made on motors of practically all sizes and types and from various manufacturers which have fully convinced me that this method is first of all, far more consistent, is easily made, and can readily be used in the shop or in the field. When the preliminary report on the test code for induction motors was first brought out, I wrote to the chairman of that committee regarding this matter, and recommended its adoption as the standard method, but allowing the use of thermometers either for special purposes, or as a check.

I am in agreement with most of the conclusions drawn from the data and reasoning presented in the papers. These conclusions and recommendations, if carried out, will prove a distinct advance in the accurate and consistent temperature rating of induction motors.

With reference to one of the proposals, however, I have been able to find very little justification for placing the new limits of temperature rise as measured by resistance five degrees centigrade above the present limit determined by thermometer. Our own careful observation over a period of eight to ten years indicates that the temperature rise of induction motors of the usual commercial types will agree very closely when taken by the resistance method and by thermometer, provided the thermometer is used with proper care as to selection of location on the winding and proper placing and protection of the thermometer bulb, or if a thermocouple is used and placed with the same consideration.

An analysis of the data presented in Mr. Summers' paper will, I believe, bear this out to a very large degree, for example in table III.

Average temperature rise, fan-cooled motors—46.9 degrees centigrade by resistance, 48.0 degrees centigrade by thermometer

Average temperature rise, enclosed (no fan) motors—40.1 degrees centigrade by resistance, 39.5 degrees centigrade by thermometer

Average temperature rise, splashproof motors—29.0 degrees centigrade by resistance, 30.8 degrees centigrade by thermometer

Average temperature rise, open motor—26.9 degrees centigrade by resistance, 29.0 degrees centigrade by thermometer

Our own tests on motors of various makes of the above types bear out these results very closely in every respect. If comparison is made of the thermometer against the resistance method, when thermometers are placed on practically any location on coils and without proper covering of the bulbs, either because of indifference or because of lack of accessibility of the parts, then the difference of the two methods may be 5 degrees centigrade or much more, up to 15 degrees centigrade or 20 degrees centigrade, as pointed out in the paper.

Mr. Summers states: "The resistance method gives results that are essentially the equivalent of careful exploration by thermometer." It should be remembered that the present method considers the rise as that of the hottest point on the winding, iron, or active parts of the motor, which can be read by thermometer, so it is only logical in making comparisons that readings as taken by the resistance method should be compared with the thermometer method when it is done in all its details with proper care. I believe it is quite evident, even with open motors, that it is more difficult and requires more time to take a sufficient number of thermometer readings to get the highest accessible temperature, than to take the reading by the resistance method. In the case of the enclosed motor, as indicated in the paper, the resistance method is the only practical one.

I do not believe any term such as "conventional allowance" should be considered in adopting a new method of rating, whether it refers to five degrees, ten degrees, or any other amount. A change to the resistance method would place the determination of temperature on an accurate basis with all data readily determinable, and easily checked.

The present standard of temperature rise, especially for closed motors, is already high and should not be changed because the resistance method is used. The resistance method is an average indication and does not indicate what is the so-called "hot spot" condition. The temperature which can be obtained by the thermometer method, if sufficient care is used, may be as high or even higher than by resistance, since it takes in all active parts as well as the windings. For this reason I feel that the present temperature rise should be maintained.

R. J. Sullivan (The Commonwealth and Southern Corporation, Jackson, Mich.): The adoption of motor temperature rise determination by resistance as the standard method, as proposed in these papers, is a step which is long overdue in the motor manufacturing industry. Manufacturers have been measuring temperature rise by resistance for years as a check on thermometer measurments, but the results obtained with the thermometers have largely been relied upon in rating motors and making test reports, due to the fact that this method has been specified as standard.

The writer, having been responsible for motor design and testing in several organizations, and being now associated with an organization which applies many motors, has long believed that motor temperature rise obtained by the resistance method is simpler, more consistent, and more accurate than that obtained by the thermometer method, and that the resistance method should be adopted as the standard method. Mr. Summers and Mr. Potter have made a valuable contribution to the industry by providing quantitative proof to support their recommendations.

When the resistance method is used with the proper equipment and precautions, persons not associated with the tests can rely on the reported results or check them, without the uncertainty which accompanies results obtained by thermometer measurements in regard to the location and method of application of the thermometers, presence of drafts, thermometer preheating and time of application after shutdown, and other factors.

An organization which purchases motors in any considerable quantity can better afford to invest in the necessary equipment for accurate resistance measurements than to maintain skilled test personnel for the intelligent application of thermometers in heat runs. The resistance method is a much more convenient and consistent method for the motor consumer to use, especially in the case of enclosed or built-in motors, and it would be of considerable value to have these tests consistent with the methods used by the motor manufacturers as a basis for ratings, guarantees and test reports.

The precautions emphasized in the papers in regard to taking "cold resistance" are important and should not be overlooked by anyone checking motor temperature rise by the resistance method. A motor may be brought in for test from an unheated storeroom where the ambient temperature is 20 degrees centigrade or 25 degrees centigrade less than that of the test room, or a field test may be made before a machine has thoroughly cooled after previous operation in service. The resistance changes approximately one per cent for each $2^1/_2$-degree centigrade difference in temperature.

R. E. Hellmund (Westinghouse Electric and Manufacturing Company, East Pittsburg, Pa.): The paper by Mr. Summers recommends a change from the thermometer to the resistance method of measuring temperature-rise in our various commercial standards (see also reference 7 of the paper). There is no question as to the desirability of making this change for some types of machines, but it is doubtful that a wholesale change, including large machinery, is justified at this time. Whenever and wherever the change is made, a question immediately arises as to the value which should be used for the permissible temperature-rise by resistance. The natural inclination is to establish this value by taking

Table II

	A	B	C	D
Allowable hot-spot temperature (degrees)	105	105	105	105
Allowance for ambient (degrees)	40	25	30	30
Allowance for difference between hot-spot and resistance measurement (degrees)	12	5	6	10
Per cent	(22.5)	(6.5)	(9)	(15)
Temperature rise by resistance (degrees)	53	75	69	65
Allowance for load factor (degrees)	10	0	3	0
Per cent	(23)	0	(4.5)	0
Temperature rise allowing for load factor (resistance) (degrees)	43	75	66	65
Allowance for variations in power supply (degrees)	6	3	3	5
Per cent	(16)	(4)	(5)	(8.5)
Temperature rise allowing for load factor and variations in power supply (resistance) (degrees)	37	72	63	60
Allowance for service mounting (degrees)	7	2	3	0
Per cent	(23.5)	(3)	(5)	0
Temperature rise allowing for load factor, variations in power supply, and service mounting (resistance) (degrees)	30	70	60	60
Allowance for difference between resistance and thermometer (degrees)	6	4	8	10
Per cent	(25)	(6.5)	(16.5)	(20)
Temperature rise allowing for load factor, variations in power supply, and service mounting (thermometer) (degrees)	24	66	52	50

into account the difference between the values by thermometer and by resistance determined by tests such as described in the paper. Data of this nature undoubtedly are of value and should receive due consideration; however, it should be determined whether a better method might not be to work down from the more basic value allowed for the hot-spot temperature to a permissible value by resistance. In so doing, it will not be practicable to make allowance for each of the various factors independently, but the whole problem should be considered broadly and a decision reached regarding the best philosophy of rating.

In rating and applying electric motors, a great many factors have to be given consideration, such as the permissible hot-spot value, ambient temperature, the difference between the hot-spot and resistance measurements, variations in power supply (particularly voltage), service difficulties resulting from insufficient knowledge of the expected load, differences in cooling between the test-floor and service mounting, etc. All of these factors are not merely theoretical possibilities, but can be of appreciable importance either individually or collectively. In table II of this discussion an attempt has been made under A to take all of these factors into account. The differences between temperatures are given in degrees and also in percentages, the latter in most cases being the best figure for evaluation. The allowances for all values under A are not the maximum experienced for each particular factor indicated but are values approximating the safer limit. It will be noted that this method results in a temperature-rise of 30 degrees by resistance and of 24 degrees by thermometer. Obviously, nobody would consider the adoption of these values as rating standards in view of the extensive experience available showing satisfactory all-round results with the present 40- and 50-degree ratings (by thermometer). This is simply an admission of the fact that we cannot afford to adopt methods too conservative, because it would result in an enormous economic waste.

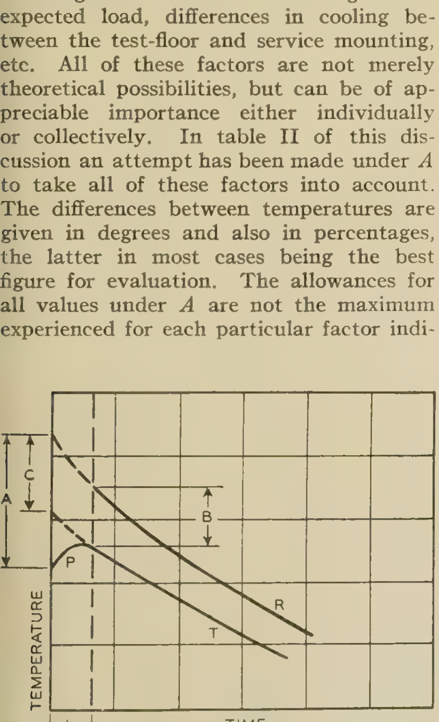

Figure 1. Cooling curves

Under B, values have been introduced approximating the average values found in practice. Zero has been given for the load factor because the actual load will be below at least as frequently as above the estimated load values. This leads to values of 70 degrees rise by resistance and 66 degrees by thermometer. It is again evident that nobody will seriously consider this practice, because experience with 40- and 50-degree ratings (by thermometer) do not indicate that the appreciably higher values given under B can be used safely. Under C, certain intermediate values have been selected more or less arbitrarily, resulting in a value of 60 degrees by resistance. There is no particular merit in the values given under C except that they lead to 60 degrees, which is the international value and one which also is now used in a number of our American standards. All of these figures indicate that the tendency which has developed to allow in the rating structure for all sorts of possibilities is impracticable.

We should, therefore, realize that in rating and applying machines there are three responsible parties involved—namely, the manufacturer, the central station, and the user—and that no one of them can be held responsible for factors which are beyond their knowledge or control. This naturally leads to a method of establishing satisfactory values for a rating structure such as shown under D. Here the equivalent ambient temperature has been assumed to be 30 degrees, a value which will cover the majority of all applications; a rather liberal allowance has been made for the difference between the hot-spot and resistance measurement values, and a moderate allowance has been made for the frequently occurring smaller variations in the power supply. The value arrived at for the resistance method is 60 degrees. With this as a background, the user knows that the machine which he buys will be suitable for the majority of reasonably normal conditions met in practice. On the other hand, if he feels that in his application he is likely to encounter exceptionally high ambient temperatures or that his power supply differs considerably from the rated value of the motor, or, again, if he is uncertain about his load or is not sure that he is applying the motor without interfering with the normal cooling and ventilating provisions, he must make allowances for such variations either through his own knowledge or by consulting available application data or the supplier of the motor.

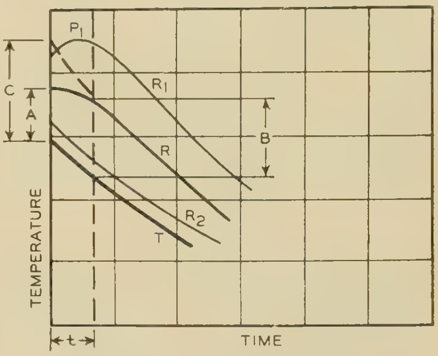

Figure 2. Cooling curves

The inadvisability of basing the temperature-rise value by resistance on certain differences between thermometer and resistance measurements is further indicated by figures 1 and 2 of this discussion. In figure 1, curve R gives the time-temperature curve (by resistance) after shutdown, and curve T is the corresponding value by thermometer. In this figure it is assumed that the time t expires before the first reading can be taken and that we are dealing with a large machine in which the copper temperatures are higher than the temperatures in the core. Both curves are extrapolated toward $t = 0$. If the thermometer is placed at the end connections, which may be the only accessible place, it will be found that the actual temperature is likely to rise for some time after shutdown, as indicated by the portion P of curve T. This means that the actual difference in operation between the thermometer and resistance values corresponds to A, while the difference at the earliest time of measurement corresponds to B. If both curves are extrapolated as shown by the dotted lines, we obtain the value C. We therefore have three widely different values to choose from, and consequently the final result will be greatly influenced by the procedure followed.

Figure 2 is intended to give similar conditions for a machine in which the core is hotter than the coil portions in the slots. Here the temperature, by resistance, of some coil portions is likely to rise slightly after shutdown, as indicated by the portion P_1 of curve R_1. Curve R_2 corresponds to the value of resistance applying for certain portions of the coils away from the core, while curve R may be considered to represent the average of all coil portions. Curve T again represents the value determined by

thermometer as it is likely to be in this case. We now can again choose from the values A, B, and C, which widely differ from one another. All of these discrepancies may not be of primary importance in per cent of the total temperature-rises, but the figures merely show that there are some difficulties encountered in an attempt to obtain the difference between the thermometer and resistance measurements with any degree of accuracy.

In the use of the resistance method for large machines, there is one inherent difficulty of considerable practical importance. Frequently the temperature of the test floor varies appreciably and rather suddenly with weather changes; however, the resistance temperatures of large machines will follow such changes only slowly. This will naturally result in errors in the cold resistance of the machine, which is an important factor in the determination of the temperature rise by the resistance method. Since it is not always possible to delay tests until the copper temperature is the same as that of the surrounding air, some discrepancies will be unavoidable, particularly in the larger machines.

C. P. Potter (Wagner Electric Corporation, St. Louis, Mo.): In his paper, Mr. Summers calls attention to the fact that "all the surface measurements on the stator windings of the totally enclosed and totally enclosed fan-cooled motors of table III were obtained with thermocouples which were well distributed in order to locate the hottest point." In other words, "the testing procedure for the enclosed machines was carried beyond the requirements of the ASA standards, because some of the thermocouples were in locations that could not have been reached by inserting thermometers through holes in the frame." He further states that "the recorded winding surface temperatures were approximately 3 degrees centigrade higher on the average than could have been obtained by the thermometer method." The following table shows the results of the temperature tests on the totally enclosed motors, including temperature readings by thermometer which were estimated by assuming that the rise by thermocouple is 10 per cent higher than the rise by thermometer.

	Totally Enclosed Fan Cooled	Totally Enclosed
(A) Average temperature rise by thermocouple of hottest winding surface—degrees centigrade	48	39.5
(B) Average temperature rise by thermocouple of hottest *accessible* winding surface—degrees centigrade	45	36.5
(C) Average temperature rise by thermometer of hottest accessible winding surface—degrees centigrade	40.9	33.2
(D) Average temperature rise by resistance — degrees centigrade	46.9	40.1
($D-A$) Difference between temperature rise by resistance and temperature rise by thermometer — degrees centigrade	6	6.9

In appendixes IV and V of his paper, Mr. Summers compares temperature by resistance with the maximum temperature observed by surface measurements on either the windings or core iron. After studying his paper, this seems to be a desirable procedure but it probably accounts for some of the variation in results reported by different manufacturers. Some engineers undoubtedly compare the temperature by resistance with the temperature of the winding surface. If this were done in table IV, the maximum differences between resistance measurements and winding temperatures (at least some of which were taken by thermometer), are eight degrees centigrade in one motor and seven degrees centigrade in two other machines. Similarly, in table IV, when all the winding surface temperatures were taken by thermometer, the differences between temperature rise by resistance and temperature rise by thermometer at full load and the various voltages are seven degrees, seven degrees, and six degrees, respectively. In other words, the differential between winding surface measurements taken by thermometer and resistance measurements seems to be about seven degrees centigrade.

In table III the temperature rise of the laminations of the enclosed squirrel-cage motors is considerably less than the temperature rise of the windings, while in the open and splashproof motors, the reverse is true. It would seem likely that open and enclosed motors of a given rating have magnetic circuits which are more or less alike, and it is probable that the flux densities in the enclosed motors are higher than in the open machines. I would, therefore, like to ask the author, whether there is a possibility that the laminations are more accessible on the open than on the enclosed motors, and whether this may account for the difference in temperatures.

Mr. Summers deserves a great deal of credit for the completeness of his treatment of the determination of temperature rise of induction motors. He has presented a wealth of information in a systematic manner and I agree with him completely in his conclusions and recommendations.

It is gratifying to have so much discussion on the paper on measurement of temperature and to have those who take part in the discussion, agree, in general, with the conclusions reached by the writer. The comments made by Messrs. Hill and Sullivan are very interesting and Messrs. Anderson, Rutherford, and Veinott have given additional data confirming the results reported in the paper. Both Messrs. Rutherford and Veinott have also suggested that the allowance for the hot spot be expressed as a per cent rather than a constant value. This, in the writer's opinion, is a very excellent suggestion and one which should be adopted.

Messrs. Alger and Kilgore have suggested methods of correcting the temperature rise back to the instant of shutdown, and Mr. Alger states that it will be possible to develop correction factors for motor temperature tests similar to those used in transformer practice. This can undoubtedly be done, but it might be well to point out that the two cases are somewhat different. In a transformer the oil circulates due to the difference in temperature between the top of his case and the bottom of the case and this circulation is not immediately interrupted when the temperature test is finished. In a motor the ventilation is produced by the fans on the rotor and this ventilation stops as soon as the temperature test is finished. In the case of an average motor the temperature of the stator iron is ordinarily less than the temperature of the extended parts of the winding. When the temperature test is stopped, the temperatures of the exposed parts of the windings decrease while the temperatures of the stator core increase, indicating that an equalization of temperature is taking place in the machine. It is, therefore the writer's opinion that the resistance in a motor winding does not decrease nearly as rapidly as that of a transformer winding after the temperature test is ended, and it is suggested that this subject be given further study.

Mr. Thomas has commented from the point of view of the motor user and while he agrees that temperature rise should be measured by the resistance method, he does not agree that any higher values of temperature rise be assigned if the change in method is approved. This again is a subject which deserves, and will have, further study before any changes are made in existing standards.

E. R. Summers: In his paper Mr. Potter has presented data and arrived at conclusions which are in close agreement with my test results and recommendations. The following remarks regarding his discussion of my paper do not represent any basic difference of opinion, but are intended only to supplement some of the points which he has mentioned.

Mr. Potter has estimated that thermometers would have indicated ten per cent less winding temperature rise than was actually obtained with thermocouples on the totally enclosed fan-cooled and totally enclosed motors of table III. Nonuniformity of testing practices among different organizations give rise to considerable variations between the temperature differentials obtained by comparing thermometer and thermocouple readings. The leads of several thermocouples can be brought out through one hole in the frame, whereas a separate hole must be drilled for each thermometer used in an enclosed motor. Consequently readings are likely to be obtained at more points with thermocouples than with thermometers, and any general comparison of the two devices usually involves differences in thoroughness of exploration as well as variations in contact with internal parts. Furthermore thermocouples can readily be placed in locations not accessible to thermometers, and dependable comparisons would require exactly the same mounting positions.

Mr. Potter emphasizes the probability that some manufacturers compare temperatures obtained by the resistance method with thermometer measurements taken on the windings only. Such comparisons are not valid, because the thermometer method as defined in the standards includes the hottest surfaces on the laminations as well as on the windings. On the majority of open and splashproof motors, the laminations are hotter than accessible parts of the

winding. To interpret comparative data from different manufacturers, it is therefore essential to ascertain if readings on laminations are included. If lamination temperatures are omitted on open or splashproof motors, the data cannot be used directly for purposes of standardization.

From my experience, I find that thermometers and thermocouples when used in exactly the same locations with similar protective coverings will give readings which usually agree within the accuracy of the test, or within two degrees centigrade, as indicated in the data for motor X in table I.

Mr. Potter has questioned the data on the totally enclosed motors of table III because the indicated temperatures on the laminations are considerably less than on the windings. Since the lamination and winding temperatures are very nearly the same on totally enclosed motors, thermocouples were placed on the windings only. The laminations were not accessible, and the indicated temperatures are lower than the actual values by the amount of thermal drop through the frame as indicated by the asterisk and note at the bottom of table III. These apparent discrepancies in lamination temperatures are indicative of the differences which may be expected when temperatures are measured only on the outside of the frame on totally enclosed motors.

Mr. Kilgore states that winding temperatures change very rapidly in the first few minutes after shutdown, and that test results by the resistance method may vary as much as six degrees centigrade if one tester gets the readings in 0.5 minute and another requires 1.5 minutes after shutdown.

Heat energy is dissipated at a much lower rate after shutdown than when operating at normal load. For a machine operating at a nominal current density of 2,500 amperes per square inch, the initial rate of change of copper temperature during the first few seconds after removing load (while motor is still coasting at full speed) is approximately $0.9\left(\frac{2{,}500}{1{,}000}\right)^2$ or 5.6 degrees centigrade per minute. However this high rate of change does not persist for two basic reasons:

1. The thermal capacity of the adjacent insulation and laminations occasions a sharp decrease in rate of change in copper temperature after the first few seconds.

2. The motor ventilation decreases as the machine is decelerated.

In many cases the motor can be stopped in less than one-half minute. The change in average copper temperature during deceleration will seldom exceed two degrees centigrade if the machine is stopped promptly, and part of this change can be accounted for by extending the resistance curve to zero time.

After a motor is stopped the rate of change in average copper temperature is usually less than one degree centigrade per minute for the first ten minutes. Figure 3 of this discussion shows the change in temperature by resistance method after shutdown on eight typical motors representing widely different ratings and types of construction. Zero time indicates the instant of opening circuit breaker to remove load. The dotted portion of each curve shows the time required to stop the motor and obtain first

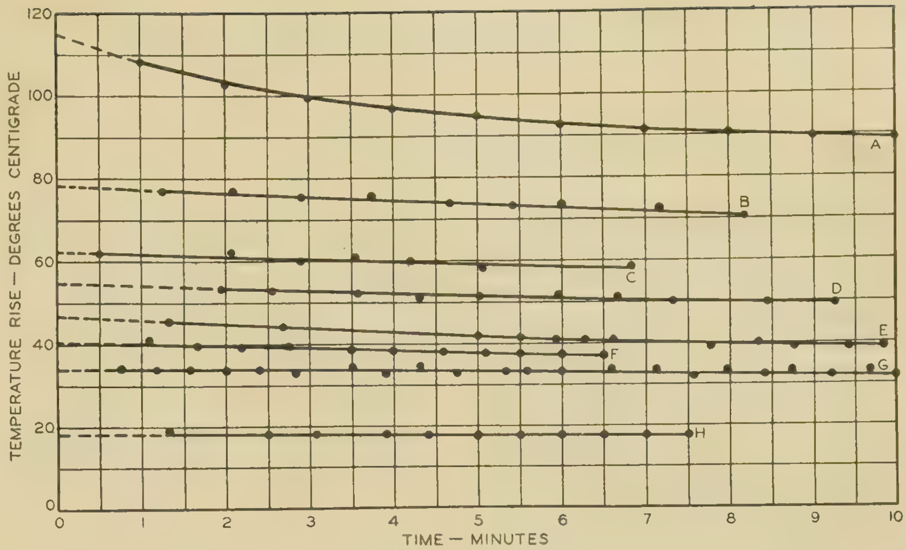

Figure 3. Curves showing change in average winding temperature after shutdown (resistance method)

Curve	Form of Motor Construction	Horse-power Rating	Speed (Revolutions per Minute)
A	Open	25	1,800
B	Splashproof	15	900
C	Totally enclosed fan-cooled	50	3,600
D	Totally enclosed fan-cooled	300	1,800
E	Totally enclosed	60	900
F	Totally enclosed fan-cooled	5	720
G	Splashproof	30	900
H	Open	40	1,800

resistance reading. Curve A is the only case where a probable change in temperature of as much as six degrees centigrade is indicated during the first minute, and this occurred on a 40-degree-centigrade-rated 25-horsepower motor that had been operated continuously at 168 per cent of normal load and which had an abnormal temperature rise of 108 degrees centigrade one minute after removing load.

To obtain reliable temperature measurements by either the thermometer or resistance methods, provision must be made for stopping the machine promptly to minimize effects of ventilation by motor fans. It is far more important to decelerate the motor quickly than it is to measure the resistance immediately after stopping, because heat energy is dissipated much faster while the motor is running (except for totally enclosed machines). The difference in resistance readings taken 1.5 minutes instead of only 0.5 minute after stopping motor is seldom more than would correspond to one degree centigrade on enclosed motors or two degrees centigrade on open machines.

The average differentials of 10 degrees centigrade to 15 degrees centigrade which Mr. Kilgore reports between thermometer and resistance methods are much greater than I have observed, and this disagreement in results is probably caused by (1) differences in thermometer exploration, (2) omission of lamination temperatures, and (3) differences in estimated change in copper temperatures during deceleration. Mr. Kilgore's comments would indicate that the 15-degree-centigrade conventional allowance is not sufficient for thermometers, if the resistance readings indicate 15 degrees centigrade higher average temperature than the thermometer method.

Mr. Hellmund's discussion is quite broad, and a number of his general comments apply equally well to all methods of temperature measurement. In the table where he has evaluated the various factors concerned with motor rating, the combined effects of service factor, variations in power supply, and allowance for service mounting are superimposed to arrive at the "conservative" limit of 30 degrees centigrade resistance rating in column A for general purpose motors. Under the present standards the 15 per cent (or ten degrees centigrade) service factor is imposed only for normal conditions of power supply and ventilation, and is not intended to apply for abnormal operating conditions. However, his evaluation of these different factors is certainly of pertinent interest.

Since the American and AIEE standards now specify the thermometer method, any general change to the resistance method necessarily involves direct differentials between the two methods. The cooling curves which Mr. Hellmund presents indicate that these differentials are quite indefinite and that widely varying values may be obtained. These curves make the problem appear more difficult than it really is.

The temperature by the thermometer method as now defined in the standards is the highest observed reading on either laminations or windings before or after shutdown, and would correspond definitely to the maximum point on Mr. Hellmund's curve marked T which should represent the highest reading thermometer.

The temperature by the resistance method may be defined either as the temperature immediately after shutdown (within a specified time) or as that obtained by projecting curve R to zero time. As previously stated the effect of projecting curve R to zero time seldom changes the result by more than two degrees centigrade if the motor is stopped promptly, and therefore the values from which one must choose are not widely different.

In the case of large or high-speed motors where no provision can be made to stop them quickly, the resistance method as

Duty Cycles and Motor Rating

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Synopsis: The horsepower rating of a motor carries a dual implication—first, torque ability; second, temperature rise. The two are frequently confused. In selecting motors for duty-cycle jobs the two concepts should be considered separately. The motor rating and type should be chosen to fit the torque requirements of the job. The proper time rating or service factor to associate with this horsepower rating to insure satisfactory insulation life can be determined from the duty cycle. Use o^c^ oversize continuously rated motors instead of short-time-rated motors to secure high torque ability on variable load jobs imposes an economic loss. Frequent starting and reversing or starting high inertia loads imposes a temperature hazard frequently greater than heavy overloads. Horsepower rating as now understood is not a satisfactory criterion of a motor's reversing ability. These topics are developed by means of simple hydraulic analogies.

BY DEFINITION, one horsepower means 33,000 foot-pounds per minute, equivalent to 746 watts. By convention and rules, the term horsepower when used as a motor rating, carries additional implications. In particular, the rating implies first, torque ability, and second, temperature rise. The horsepower rating suggests the ability of the motor from a torque standpoint to start, accelerate, and carry an overload of some magnitude. Also, the motor will carry steady rated load, or a specified overload for a specified time without exceeding a stated temperature rise.

Paper number 39-59, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 1, 1938; made available for preprinting December 31, 1938.

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Neither of these implications, nor any rules or generally known conventions, clearly suggest what a given motor will do on a duty-cycle job with a varying load, particularly if starting or reversing constitutes a regular part of the duty cycle. Sometimes, unduly large and costly motors are used to secure high torque ability for a temporary overload. A short-time-rated motor, for example one hour, 50 degrees, of correct size and rating would be more economical. However, if frequent starting or reversing or if starting a heavy flywheel load constitutes a regular part of the duty cycle, there is a distinct tendency to "undermotor" because there is not a general recognition of the heavy overload imposed by such service.

This paper is primarily concerned with what performance may ordinarily be expected from average polyphase squirrel-cage induction motors like those used on machine tools and similar applications when the load is not steady and continuous.

Torque Ability

In selecting a motor for a particular job, it is expedient and conducive to obtaining better performance or smaller motors, to consider the two concepts, torque and temperature, separately. First choose the horsepower rating from a torque standpoint only without any consideration of temperature. Temperature rise is considered separately later. The motor must have sufficient starting

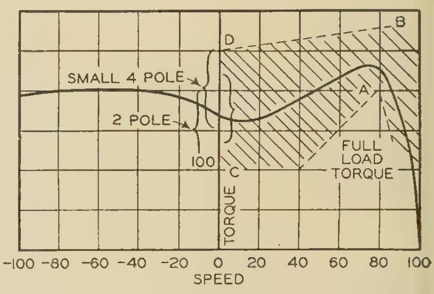

Figure 1. Typical speed-torque curve class A and B motors

torque to start the load under the adverse conditions of low line voltage and reasonable overload. The motor must have sufficient accelerating torque to bring this load up to full speed. Sometimes the distinction between starting and accelerating torque is not made and higher starting torque than necessary is specified because of experience with another type motor which had low accelerating torque. The motor must have sufficient breakdown torque to carry a reasonable overload with low line voltage without an excessive drop in speed. More than adequate torque ability is an extravagance paid for by other characteristics or increased size.

There are four readily available classes of squirrel-cage motors:

Class *A*. Normal starting torque, normal starting current (usually made with a single-cage rotor)

Class *B*. Normal starting torque, low starting current (frequently made with a double-cage rotor)

Class *C*. High starting torque, low starting current (usually made with a double-cage rotor)

Class *D*. High starting torque, high slip (almost always single cage)

In this paper we will generally not distinguish between classes *A* and *B* as

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previously described is not directly applicable, and imbedded detectors are recommended. Mr. Hill has mentioned the possibility of opening the power circuit momentarily to obtain resistance readings while the machine is running. We have taken some data in this manner which was consistent in every respect. Our experience with this method of taking resistance readings is not yet considered conclusive, but such a procedure does offer a possible means of testing large motors which cannot be decelerated in a reasonable time.

Mr. Hellmund's comments on the difficulty in obtaining dependable cold resistance temperatures are well stated. In general it is more difficult to obtain the "cold" winding temperature within one degree with thermometers when the ambient is changing, than it is to measure the ohmic resistance with comparable accuracy using a suitable double bridge. Mr. Sullivan and Mr. Hill have also carefully discussed the importance of accurate cold temperature measurements.

Mr. Thomas has objected to any addition to the present thermometer ratings to obtain the proposed new ratings by resistance. The suggested increase of five degrees was not intended to represent any expected difference between resistance data and the temperatures which could be obtained by a thorough exploration with thermometers (or thermocouples). This increase is suggested because the resistance method avoids the low readings sometimes obtained by thermometer, and 60 degrees centigrade rise by resistance is considered to be fully as conservative as 55 degrees centigrade rise by thermometer. On the basis of 40 degrees centigrade ambient and 105 degrees centigrade limiting hottest-spot temperature, a resistance rating of 60 degrees centigrade provides a hot-spot allowance of 5 degrees centigrade which tests indicate to be a representative value for totally enclosed motors.

It is very pleasing to note the close agreement between the discussions of Messrs. Potter, Hill, Sullivan, and Thomas. Although not in complete agreement on the proposed differentials and ratings by resistance, everyone apparently agrees in principle that the resistance method is desirable for enclosed and inaccessible machines.

they are usually interchangeable in application.

To establish a reason for selecting a particular horsepower rating and a particular class motor to do a particular job, typical speed-torque curves for average motors are shown in figures 1, 2, and 3. Data are intended for illustrative purposes only, as individual motors may depart appreciably from the average. Points A and B, figure 1, show minimum and maximum logical values for breakdown torque. It is assumed that any motor should carry some overload, say 25 per cent at 10 per cent reduced voltage, with a reasonable margin for safety, say 20 per cent. Since the motor's torque ability is proportioned to the line voltage squared, we require $1.25 \times 1.2 \div 0.9^2 = 1.85$ or 185 per cent breakdown torque, as tested under normal

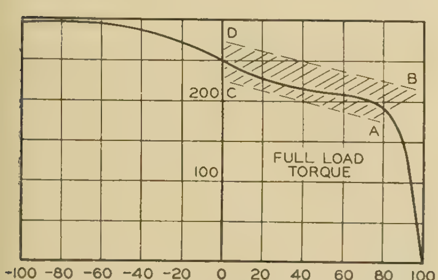

Figure 2. Typical speed-torque curve class D motors

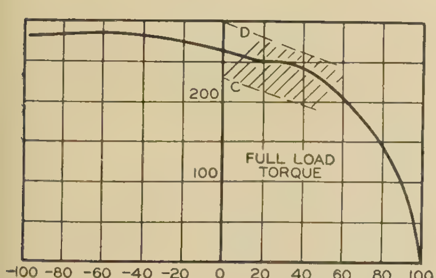

Figure 3. Typical speed-torque curves class D motors

conditions. This is close to the 200 per cent minimum breakdown torque which is now standard for class A and B motors. It is assumed that a motor would not ordinarily meet the minimum requirements for the next higher standard rating. In the lower range, an average step or ratio of successive horsepower ratings is about 140 per cent. This establishes point B, figure 1, as 280 per cent for the greatest breakdown torque to be logically expected.

Parenthetically, it should be noted that stray load losses constitute a part of the load on the motor. They appreciably reduce the breakdown torque below the value given by formulas in handbooks which do not recognize stray load losses.

In class C motors, breakdown torque is sacrificed to obtain increased starting torque. See figure 2 showing typical values.

With regard to starting torque, the writer knows of no process of reasoning which will suggest a logical value purely on the basis of the horsepower rating. Suitability to the usual application and the necessary compromises of design have established starting torque values for motors as now made. Extravagant starting torque can be obtained only by sacrifice of other desirable characteristics such as efficiency, power factor, or breakdown torque.

In figures 1, 2, and 3 a portion has been shaded to indicate the expected variations of different motors and of different ratings. Points C and D, figure 1, show the total range of variation in starting torque of class A and B motors. For different motors of a particular rating the range is of course smaller covering the lower, middle, or upper part of the range shown in figure 1.

Class A or B motors should be used for jobs not requiring unusually high starting torque, not started or reversed frequently, or not used to start a heavy inertia load. As will be shown later, there are many cases where a short-time-rated class A or B motor can be used economically.

Class C motors are intended for jobs requiring exceptionally high starting torque such as reciprocating compressors. In the smaller sizes, efficiency may be sacrificed, and in the larger sizes, breakdown torque must be sacrificed to obtain this higher starting torque. As will be shown later, class C motors can be reversed more frequently than class A or B.

Class D motors are used for a variety of jobs, including:

1. Hoists and similar jobs where maximum starting torque is required with sacrifice in ordinary efficiency.
2. Frequent reversing.
3. Starting, stopping, and reversing high inertia loads such as extractors and centrifuges.

In tentatively selecting the horsepower rating and best class of motor from a torque standpoint, the job should be considered with regard to its requirements for starting, accelerating, and breakdown torque. To allow for a possible 10 per cent drop in line voltage, the rating must be high enough so that 81 per cent of the breakdown torque of the motor is safely higher than the maximum load. (Breakdown torque is proportioned to the line voltage squared.) Also, the speed with this maximum load must not be unduly low. If the rating is three quarters of the maximum load, these conditions are generally fulfilled. Class A motors can be temporarily overloaded somewhat more. If unusually high starting torque is not necessary, a class A or B motor is of course indicated. If higher starting torque is required, a class C motor is proper. The horsepower rating must be such that 81 per cent of the starting torque will always start the load. Again, we allow for low line voltage. Whichever of these two specifications, starting or breakdown torque, indicates the higher rating is the factor which determines the rating from a torque standpoint. With polyphase motors and other motors not having low points in the torque curve, extravagant starting torque is not necessary to secure adequate accelerating torque.

Temperature

We must now consider the motor from a temperature standpoint. Is the tentatively selected rating cool? If very cool what short-time-rated motor can be used? As is well known, temperature is limited because insulation deteriorates more rapidly at high temperatures. The insulation should not wear out while the motor is otherwise modern and usable. Insulation deterioration is a function of time as well as temperature. Thus the temperature rise during a short period may safely be somewhat higher than conventional values if compensated by much longer periods during which the temperature is much lower.

The newer synthetic insulation ma-

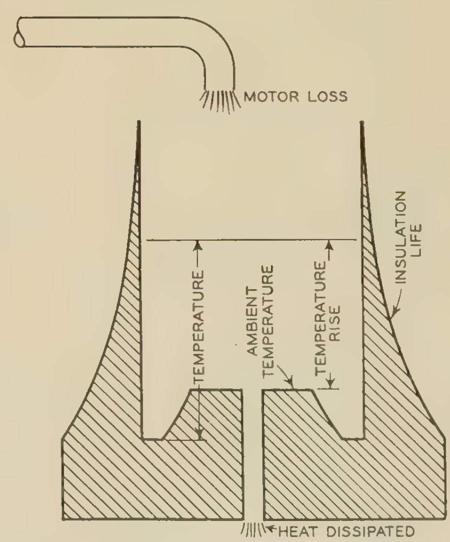

Figure 4. Hydraulic analogy illustrating temperature rise

terials now being used in motors have longer useful life than materials used a few years ago. Today enamel in combination with cellulose is generally used for wire insulation. This enamel is better and more generally used than when present temperatures were standardized. With all the developmental work now being done, it is not unreasonable to expect improvements to continue.

Figure 4 presents a hydraulic analogy to explain temperature rise. (Some distortion of the laws of hydraulics is necessary to make the analogy mathematically correct.) Water flowing into the tank is illustrative of the motor losses. Watts loss in the motor is analogous to cubic feet per minute water flow. The total kilowatt-hours energy loss is illustrated by the total cubic feet of water. The height of water in the tank is analogous to temperature. The capacity of the tank is illustrative of the heat storage capacity of the motor, proportional to the summation of the weight of the parts multiplied by their specific heat (weighted for temperature differences). The outlet at the bottom is illustrative of the heat dissipation by radiation, conduction, and convection. Obviously, the water rises to a level so that the outflow balances the inflow, that is, the temperature is steady.

Note that the walls of the tank are not of uniform thickness. This suggests the time factor in insulation deterioration. The slow rusting or corrosion of the walls is analogous to wearing out of the insulation. (The scale for insulation life is distorted to keep the diagram reasonably small.) The diagrams which follow are simplified by showing a tank with a thin wall. The reader should remember that a tank like figure 4 is always implied to represent more closely the fact that insulation wears out because of high temperature for a long time.

In figure 5, the analogy is elaborated to bring in more detailed factors. Losses flow into the tank from two pipes—first no load losses and second extra losses due to carrying a load. It is a quite accurate common assumption that the extra losses due to load are proportioned to the load squared. The heat dissipation is divided into two parts—the dissipation when the motor is not running and the extra dissipation due to extra ventilation when the motor is running. Valve *A* is analogous to the line switch. *A* and *C* are tied together to indicate "turning on" the ventilation when the motor is started. Valve *B* is opened more or less to indicate more or less load.

Figure 5*A* pertains to an average normal or high-speed motor, say 1,200 to 3,600 rpm. Figure 5*B* indicates the changed proportion of the factors for a slower speed motor, say 450 to 900 rpm of the same physical size but lower horsepower rating. Note the greater losses at no load and the lesser ventilation.

Figures 5*C* and 5*D* illustrate short-time-rated motors made in the *same size parts* for which figures 5*A* and 5*B* indicate continuously rated motors. The horsepower rating is higher, the torque ability, that is, starting, accelerating, and short-time overload ability, are increased. When operated at the increased rated load, the losses are higher as indicated by the larger inflow pipes. Since the heat dissipation ability is not increased, obviously the motor cannot be operated at rated load continuously without exceeding ordinary temperature limits. According to the conventional rating, say one hour, 50 degrees centigrade, the motor is started cold and at the end of one hour the energy losses have been either stored in the parts or dissipated so that the motor has just reached its temperature limit. In typical industrial applications there is seldom such a duty cycle. Usually the motor is operated with varying load or intermittently so that after a period the motor settles down to a continuous average temperature. In the analogy valves *A* and *C* may be open all or part time to indicate either continuous or intermittent operation. Valve *B* is turned off and on varying amounts according to the actual load. The height of fluid rises to an average level which is maintained because on the average just as many cubic feet of water flow in as flow out.

Normal and high-speed one-hour motors will generally carry a continuous load as high as the continuous rating associated with the size parts. For example, a 5-horsepower continuous motor and a $7^1/_2$-horsepower one-hour motor are usually made in the same size parts. The $7^1/_2$-horsepower one-hour motor will generally carry continuously as much load as the 5-horsepower continuous motor. However, the torque ability is increased to take care of short overloads and abnormal conditions. Thus the one-hour motor is well suited to many industrial loads.

A slow-speed one-hour motor may not carry, from a temperature standpoint, as much load as the lower rated continuous motor in the same parts. Figure 5*D* makes this clear. If the motor is on the line continuously, the no-load loss is high. Even though valve *B* is partially closed, the total loss may be greater than the heat dissipating ability.

The short-time conventional temperature test is not always an exact criterion of the merit of a motor. Figure 6*A* is analogous to a motor which is large and heavy for its rating with inferior ventilation. At the end of the conventional test, the temperature is not excessive since the motor has excess heat-energy storage capacity (that is, a large tank). On an actual intermittent load the motor operates hot due to poor ventilation

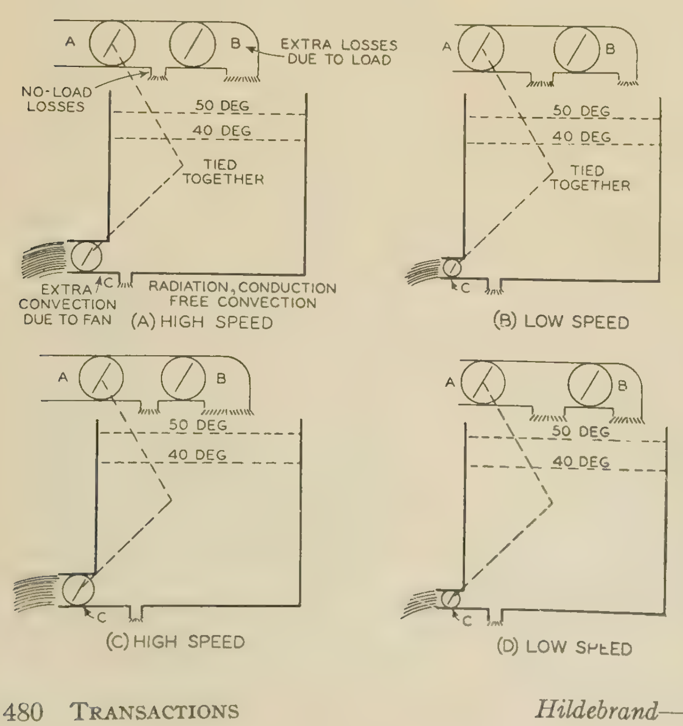

Figure 5. Hydraulic analogy illustrating factors affecting temperature rise

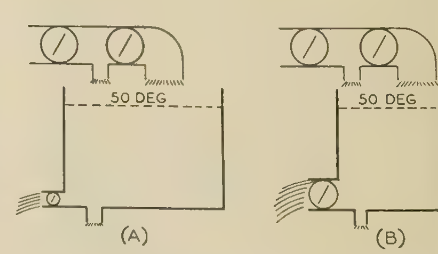

Figure 6 (right). Hydraulic analogy illustrating heavy poorly ventilated and light well-ventilated short-time-rated motors

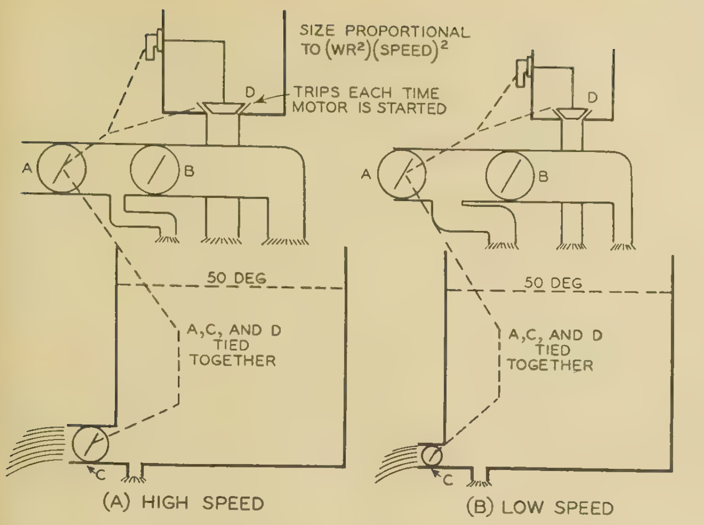

Figure 7. Hydraulic analogy illustrating extra losses due to starting

(small outflow from tank). Figure 6*B* indicates a lighter-weight well-ventilated motor which when tested by the conventional short-time test seems to be inferior as the temperature is high. On the actual varying load it operates cool because it is well ventilated.

Service Factor

Probably the economically sound field for these intermittent-duty motors is broader than their present range of use. Lack of a definite descriptive nomenclature which clearly indicates the ability of the short-time-rated motor on an intermittent load is possibly one of the largest detriments to the more extended use of these smaller, lighter, high-torque-ability motors. Obviously a slow-speed short-time-rated motor will not carry as much load with a safe temperature as a higher-speed motor which passes the same conventional test. It is not to be expected that all users of motors are sufficiently informed regarding details of design and application to use safely these more economical motors without fear of excess heating. Thus there exists a distinct tendency to "overmotor" from a heating standpoint in order to obtain a safe margin in torque ability.

There is a term in current use, the meaning of which can be broadened to indicate more definitely what these motors will do. The frequently quoted 1.15 service factor clearly indicates that the normal motor to which it is applied will carry 115 per cent rated load without exceeding recognized safe temperatures. A service factor less than unity would indicate that the motor must be loaded less than its rating continuously in order to maintain normal temperature limits. Of course, the load may be either continuous or a varying load of the same root-mean-square value.

If the job does not include frequent starting and reversing or starting high inertia loads, there is no hard problem in selecting the motor from a temperature standpoint. Merely calculate or estimate the root-mean-square load and see that the motor rating multiplied by the service factor is at least as high. For the purpose of this paper (root-mean-square horsepower load) is defined as

$$\sqrt{\frac{\Sigma\,(\text{hp})^2 \times \text{time}}{(\text{running time}) + \dfrac{(\text{standstill time})}{\text{constant}}}}$$

For the numerator each part of the duty cycle is considered separately. Take the sum for all parts of the cycle of the square of the horsepower load multiplied by the time for this element of the cycle. The constant in the denominator is the ratio of heat dissipation running to standstill, frequently assumed to be 4. Slightly higher values may apply to high-speed motors and lower values apply to low-speed motors. For enclosed motors without fans this constant is not much greater than one.

For application purposes, normal and high speed, one-hour motors may be assumed to have 80 per cent service factor. This factor is derived by the following reasoning: The standard motor has 1.15 service factor. The one-hour rating averages 140 per cent of the continuous rating in the same size parts. The one-hour motor will dissipate continuously as much loss as the continuous motor and the efficiency is essentially constant. Hence, the service factor of a one-hour motor may be assumed to be 1.15/1.4 = 0.82. We use 0.8.

Starting and Reversing

In the above all starting and reversing operations in the duty cycle are not included. The losses due to these operations are included separately. Frequent starting and reversing or starting a high inertia load imposes a severe temperature hazard, often more severe than a heavy overload. In figure 7 our hydraulic analogy is extended to illustrate the extra losses due to starting. As is well known, the stored energy in the rotating system constitutes a fundamental unit in acceleration problems. The stored energy is 3.87 $\times$ 10^{-6} (WR^2) $(\text{rpm})^2$ watt minutes. WR^2 is in pound feet squared. Each time a motor is started one of these units is supplied to the rotor in the form of heat. There are additional losses in the stator. Total losses for starting class *A* and *B* motors will average 2 to $2^1/_2$ times the rotor loss. With class *D* motors total losses will be $1^1/_4$ to $1^1/_2$ times the rotor loss. Class *C* motors have intermediate values.

In our hydraulic picture, figure 7, each time the motor is started, valve *D* is tripped, thus increasing the average inflow, analogous to increasing the average loss. Of course, valve *B* must be closed part of the time or the water will rise higher in the tank, that is, the ordinary load must be decreased to keep the temperature down to what it is for normal operation. Figure 8 shows a picture for reversing a motor. It differs from figure 7 in that the sudden inflow is four times as much.

The accelerating or reversing loss is proportional to the total WR^2 of the rotating system, including the motor and its load and proportional to the number of equivalent reversal per minute counting a start as one-quarter reversal.

We have not shown a picture for operation of a short-time motor, say one hour, on a reversing job. In fact, the terms become ambiguous. The motor is seldom

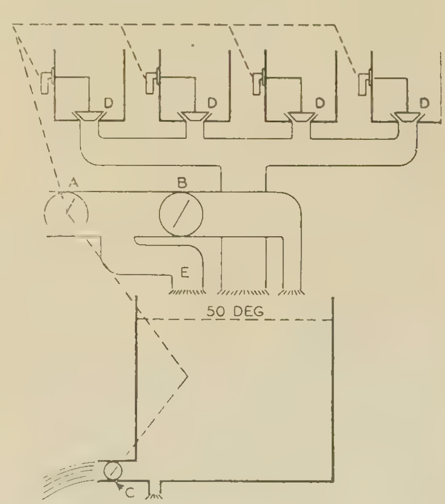

Figure 8. Hydraulic analogy illustrating extra losses due to reversing

Table I

Assumed		3,600 RPM			1,800 RPM			1,200 RPM					
			Assumed			Assumed			Assumed			Assumed	
Horsepower	Efficiency	$(WR^2)N$	WR^2	N	$(WR^2)N$	WR^2	N	$(WR^2)N$	WR^2	N	$(WR^2)N$	WR^2	N
2	82	0.84	0.2	4.2	3.4	0.6	5.6	7.6	1.5	5.0	13	2	6.7
5	85	1.7	0.5	3.4	6.8	1.5	4.5	15	3.8	4.0	26	5	5.2
15	88	3.9	2.0	1.9	16	6.0	2.6	35	15	2.4	62	20	3.1

if ever operated for only one hour as the rating implies. It is in fact used for an all-day job. The load may be light and variable but the operation is continuous. Hence, reversing service should be specified in terms of ultimate temperature.

What is the magnitude of these extra losses due to reversing? No reasoning based solely on the implications of the rating will indicate the reversing ability of a particular motor without knowledge of some of the details of design. We can assume that if the reversing losses are greater than the normal losses at full load, the motor is carrying the equivalent of an overload and normal temperature margins are reduced.

Let us work out a specific case, say of a class A five-horsepower, 1,800-rpm motor with 85 per cent full-load efficiency and with total WR^2 including load, 1.5 pound feet squared. Assume that the total reversing losses are $2^1/_4$ times the rotor losses. The loss in watt minutes for one reversal is:

$$4 \times 3.87 \times 10^{-6} \times 1.5 \times (1800)^2 \times 2^1/_4 = 168$$

The loss due to normal operation at 1.15 load, as permitted by the service factor is:

$$5 \times 1.15 \times 746 \times 0.15/0.85 = 767 \text{ watts}$$

Thus one interpretation of the rating indicates that the motor can be reversed with this inertia, 767/168 = 4.5 times per minute without exceeding normal temperature.

A general formula based on this reasoning is:

$$(WR^2)\,(\text{rpm})^2N = \frac{4.82 \times 10^7 \text{ (service factor) (horsepower rating) } (1 - \text{eff.})}{K \times \text{eff.}}$$

K is the factor dependent on design, mentioned above. Note that for a given speed $(WR^2)N$, that is, the product of the moment of inertia and the number of reversals is a constant. This is well verified by many tests. Note also that this is an optimistic estimate of reversing ability. We have assumed that the motor will dissipate as much loss due to reversing as the losses due to continuous operation. If reversed very frequently, the ventilation is impaired as the average speed of the ventilating fan is decreased. This is analogous to continually opening and closing outlet valve C, figure 8. In deriving the formula for losses due to reversing, we have neglected friction and windage losses, core loss, and magnetizing current together with extra I^2R loss in the stator caused thereby. This is analogous to closing permanently spigot E, figure 8. For high-speed motors, this is not serious. However, it is a considerable factor for slow-speed motors. In our analogy the tank is already partly filled by these neglected losses. Only the remainder is available for reversing losses. Thus while the number of reversals with a given WR^2 is increased with lower speeds, the increase is not proportional to the square of the speed ratio as suggested by the above approximate formula.

Table I shows reversing ability of a variety of class A motors in accordance with the assumptions above.

The assumed WR^2 includes both motor and load.

N is the number of reversals per minute to give the same losses as normal operation at 115 per cent load.

This table has been prepared primarily to indicate that frequent reversing of standard general-purpose motors constitutes an overload beyond the implications of the name plate. There is not a general recognition of how much this overload is. Of course more reversals than tabulated can be obtained if the inertia of the load is small, thus reducing the total WR^2. Other kinds of motors are better adapted to frequent reversing. The class C motor will give distinctly more reversals and still maintain good efficiency in the operating range. Class D motors will give maximum number of reversals but the efficiency is lower at normal speed.

Of course, special motors can be made primarily for reversing service to give many more reversals. The principles involved are well known to designing engineers. No discussion of these is included here, as this paper is primarily concerned with the use of general-purpose motors.

It should be noted that horsepower rating as generally understood is an unsatisfactory criterion of reversing ability. In fact, it may be distinctly misleading. A small motor will give more reversals than a larger homologous motor if the load WR^2 is small. In general, those factors which are conducive to obtaining high reversing ability are exactly contrary to maintaining the normal margins for normal operation usually associated with a given horsepower rating. In fact, strange as it may sound, a lower horsepower rating is frequently consistent with greater reversing ability. One such case is associated with the reversing of slow-speed motors. See figure 8. The tank is already partially filled by no-load losses from spigot E. If no-load loss fills the tank three-quarters full, only one-quarter is left for reversing. Now if the torque ability is decreased to two-thirds of its former value, the tank is only half filled by no-load loss leaving half a tank for reversing. Thus, the reversing ability from a temperature standpoint is doubled, but the normal margin in torque ability is sacrificed.

Starting High-Inertia Loads

We have discussed frequent starting and reversing where the instantaneous temperature is close to the average. Starting high-inertia loads such as extractors imposes extra transient temperatures higher than the average. The average temperature rise is determined by the same considerations as for rapid reversing jobs. Figure 9 shows a hydraulic analogy. It differs from figure 7 in that the tank is divided into three compartments to simulate the separate heat storage capacity of the stator and rotor windings and core. The three compartments are connected by ports so that ordinarily no great difference in level can exist, that is, the temperatures of the parts of a motor are quite uniform for ordinary operation.

In figure 9 the extra inrush of water into the stator and rotor compartments simulates the extra starting loss in the

motor due to starting a high-inertia load. The rotor losses flow into the rotor reservoir and the stator losses into the stator compartment. The loss is proportional to the load WR^2, hence high. All compartments are already partially filled. The extra inrush may cause one to overflow before the water can run into the others or be dissipated. Consider for example, the rotor losses which are: $3.87 \times 10^{-6}(WR^2)(\text{rpm})^2$ watt minutes. The size of the rotor reservoir is (weight of rotor windings) $\times$ (specific heat) $\times$ (safe temperature). What is a safe rotor temperature? Some of the factors which must be considered are melting point, latent heat of fusion, tensile strength at high temperatures, deterioration of alloys by change in composition or crystalline structure. It is a coincidence that the size of the rotor reservoir is about the same for the different commonly used materials. Copper will stand high temperatures but the weight for satisfactory characteristics (high resistance) is low. Aluminum melts at a lower temperature but it has outstandingly high specific heat and latent heat of fusion. Brass is heavy for a given resistance but it will not stand repeated high temperatures.

The transient stator temperature is determined by similar details of design. Of course, even transient temperatures must be held to values which will not wear out the insulation too rapidly. It is quite difficult to determine the peak transient temperature. In figure 9 we indicate the analogy to a thermometer inserted on the stator winding. A thermometer reads a temperature because its mercury gets hot and expands. Thus there must be a thermal lag in reading with rapidly changing temperatures as illustrated by the small passage between the thermometer and stator reservoirs.

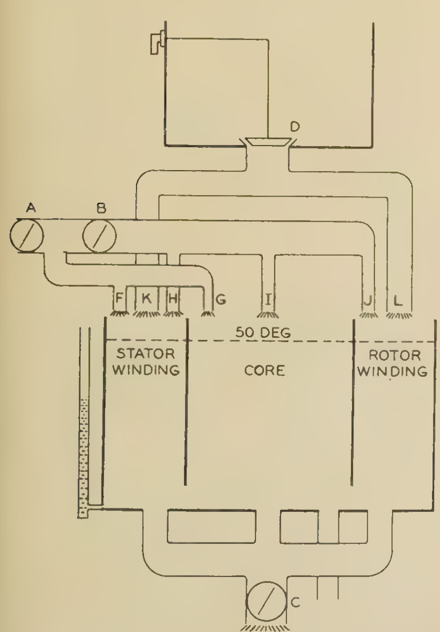

Figure 9. Hydraulic analogy illustrating extra losses due to starting a high-inertia load

These details have been discussed to indicate that design details rather than horsepower rating determines the ability of a motor to start and operate machines with high inertia. Standard motors are not ordinarily used for such service. Standard motors are designed for good characteristics at normal speeds. What ability they have to reverse frequently or to start high-inertia loads is a variable by-product of design.

Built-in Motors

A short statement is necessary regarding the operation of any motor on any job when that motor is built in as a part of a complete machine. In our hydraulic picture, the motor designer controls the size of the inflow spigots, *A*, *B*, and *D*. The machine designer controls the size of the outflow spigot. The user determines how frequently and how much the valves are opened. No one of the three can say how full the tank will be. This indicates that the performance of such motors from a temperature standpoint is a matter of divided responsibility.

Summary

1. When selecting motors for duty-cycle jobs, the two concepts, torque ability and temperature rise should be considered separately. The class or type and the rating should be such that there is no deficiency or undue extravagance in starting, accelerating, or breakdown torque ability. If the root-mean-square load is appreciably less than the motor rating required from a torque standpoint, a proper short-time-rated motor is economical.

2. Frequent starting and reversing or starting high-inertia loads frequently imposes the equivalent of an overload from a temperature standpoint.

3. The data and analogies given in the paper show that performance of a motor on starting, reversing, or other duty cycles is not clearly evident from the horsepower rating alone. The usual method of giving a motor a short-time rating is also inadequate for determining its performance on duty-cycle jobs.

4. It is suggested, therefore, that motors for duty-cycle service be given horsepower ratings representative of their over-all torque ability regardless of heating. It is suggested that service factors be used to indicate the load they can carry continuously within their proper temperature limits. Under this plan, a motor now rated one hour 50 degrees, would become a motor with the same horsepower rating but with, say, 0.8 service factor. This proposal forms a logical extension of the present plan of giving service factors higher than unity to general-purpose motors applied in continuous service on special applications.

Appendix A

Rotor loss during speed transients

$$T = J\frac{d\omega}{dt}$$

$$Tdt = Jd\omega$$

Rotor power loss $= T(\omega_s - \omega)$

Rotor energy loss

$$= \int_{T_1}^{T_2} T(\omega_s - \omega)dt$$

$$= J\int_{\omega_1}^{\omega_2}(\omega_s - \omega)d\omega$$

$$= J\left[\omega_s\omega_2 - \omega_s\omega_1 - \frac{\omega_2^2}{2} + \frac{\omega_1^2}{2}\right]$$

If $\omega_1 = 0$, $\omega_2 = \omega_s$, that is, start from rest and accelerate to full speed, rotor energy loss $= J\omega_s^2/2$.

If $\omega_1 = \omega_s$, $\omega_2 = -\omega_s$, that is, a complete reversal, rotor energy loss $= 2J\omega_s^2$.

Converting to watt minutes, WR^2 in pound feet squared and revolutions per minute, watt minutes rotor energy loss for one reversal is

Watt minutes $= 1.55 \times 10^{-5}(WR^2)(\text{rpm})^2$

T = torque in pound feet
J = moment of inertia in pound (gravity) feet^2
ω = angular velocity in radians per second
ω_s = synchronous angular velocity
WR^2 = pound feet^2
t = time
T_1 and T_2 = initial and final time

Discussion

C. G. Veinott (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Mr. Hildebrand's paper showing how difficult it may be to predict the temperatures in a motor of varying duty cycle suggests how fortunate indeed is the builder and user of fractional-horsepower motors. The maker of refrigeration or air-conditioning equipment invariably builds one or more samples of his appliance which can be subjected to all kinds of tests in the laboratory, both at elevated and at subnormal ambient temperatures. Usually these tests involve the use of a large number of thermocouples and one more can readily be added to the motor winding. Thus it is possible to determine by laboratory test, just what the winding temperatures of the motor will be in service. The problem is even simpler for the refrigeration manufacturer if he uses a motor properly equipped with an inherent overheating protector which allows the maximum useful output to be obtained from the motor windings, without endangering the windings whatsoever.

Temperature Limits Set by Oil and Cellulose Insulation

CHARLES F. HILL
MEMBER AIEE

Synopsis: The life of cellulose insulation in oil has been investigated as a function of temperature under conditions of free access to oxygen and also in an inert atmosphere. Temperatures up to 140 degrees centigrade have been used. An attempt has been made also to study the life of oils as a function of oxygen concentration in an actual transformer at various temperatures. The amount of oxygen to produce a given acidity was also determined. On the basis of these data on oils, an attempt has been made to calculate rates of oxygen absorption by oil which may be used to estimate the relative deterioration in transformers of other dimensions. The results show cellulose deteriorates only mechanically, retaining its electrical properties. It is subject to both temperature and oxidation effects, the temperature effect, of course, taking place above 105 degrees centigrade.

THE continuous rating and also the overload rating of electrical apparatus is largely determined by the insulation. Fortunately, in transformers the very large heat capacity of the cooling liquid plus a high rate of heat transfer to the liquid from the metal parts permits a high overload for short periods of time, but even there, the solid insulation should be protected from too excessive temperatures. It must be recognized that a large fraction of the strength of the solid insulation can be lost in a short time at temperatures which can exist under overload condition. This has been recognized in some instances by the addition of protective devices, thermally operated.

The temperature at which these devices should operate and also the continuous operating temperatures, which would permit a reasonable life of the insulation, are as yet rather arbitrarily fixed by accumulated experience and short-time tests. Conclusions from operating experience are not based on ideal conditions. The amount of insulation deterioration permissible is also an undetermined factor in predicting insulation life. Montsinger[1] and Bush[2] have attempted to determine rates of deterioration which could be extrapolated with time to predict the life of insulation as a function of temperature. A recent paper by Putman and Dann[3] will also be of interest, particularly from the standpoint of overload temperatures.

It is not the purpose of this paper to draw very definite conclusions as to temperatures permissible, but rather to separate the deterioration processes which affect oil and cellulose insulation. Some idea of the temperature limits imposed by these separate processes may be obtained and possibly some conclusions can be drawn from the combined effects. An attempt has been made to cover both ideal and adverse conditions of operation.

Insulating oils and cellulose have been used almost exclusively in transformer equipment as insulation. Because of this and because of their very well-known limitations, an enormous volume of research has been done on these materials, which, when we consider the extent of the research and the improvements in the materials, is somewhat discouraging. Their limitations remain the same. The conclusion is again, that improvements in their behavior in transformers will be gained largely by providing better conditions under which they are to operate.

All of the materials to be considered here are organic in nature, but they differ somewhat in their behavior toward conditions existing in transformer operation. For example, the cellulose molecule contains hydroxyl groups which under severe temperature conditions, may be split off as water. "Chemically combined water" is the term usually applied to such hydrogen-oxygen combinations. Oils, on the other hand, are not subject to such deterioration as they do not contain oxygen in their pure state. They contain only hydrogen and carbon, but are capable of reacting with oxygen to form peroxides and organic acids and other deterioration products. Some of these oxidation products may also polymerize and sludge, a final product, is the result. The peroxides in particular and perhaps some of the low-molecular-weight acids, react with cellulose to cause its decomposition.

Oils in the absence of oxygen can withstand temperatures far in excess of any permissible operation conditions, but the presence of oxygen becomes a serious handicap. The results to be presented

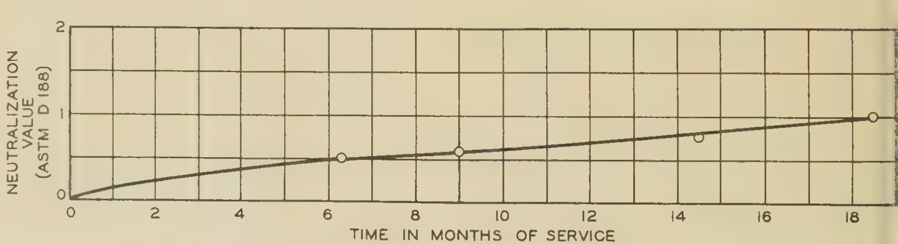

Figure 1. Acidity of transformer oil at 85 degrees centigrade—allowed to breathe once per week

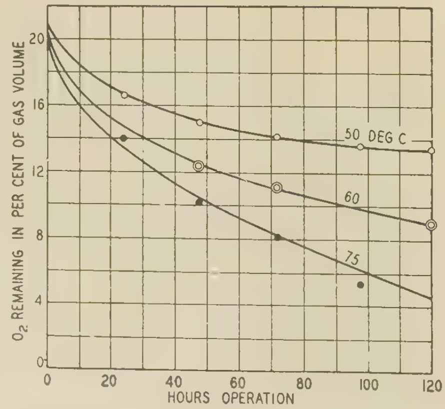

Figure 2. Residual O_2 in gas space

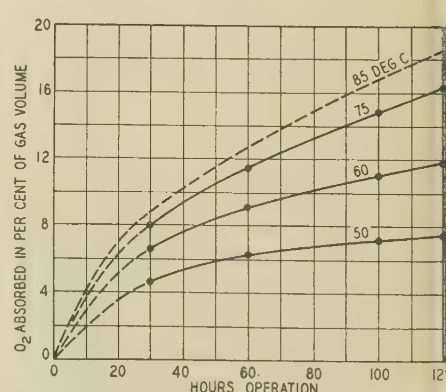

Figure 3. O_2 absorbed from gas space

Paper number 39-16, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 1, 1938; made available for preprinting December 8, 1938.

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The author is indebted to J. G. Ford of the Westinghouse research laboratories for much of the experimental data.

1. For all numbered references, see list at end of paper.

are given with the intention of emphasizing the oxygen hazard.

The experiments which concern oil alone are of interest in that they show a new method of attacking the problem of determining rates of oxidation. They also apply directly to oil in transformers which is somewhat more convincing than laboratory tests. The oxidation also takes place by contact of oil with air as it exists in the transformer. Two transformers were operated with the same conventional transformer oil. Figure 1 shows the acid accumulation in a 100-kva unit containing 93 gallons of oil, for 19.5 months with approximately one day per week off load. The one milligram KOH acidity was thus produced in approximately 16 to 17 months actual time at 85 degrees centigrade. Its oxygen absorption was 0.48 cubic foot per week or per cycle. The surface of the oil had an area of 3.47 square feet, or an average of 0.138 cubic foot of oxygen per square foot of oil area per week.

The second transformer was operated until the oil had passed its initial latent period when the gas space was blown out and sealed with a fresh air supply. It was then operated for several days, the residual oxygen in the gas space being determined periodically.

Figure 2 shows the data for 50, 60, 70 degrees centigrade for 120 hours and figure 3 is the same data graphed in terms of oxygen absorbed. The 85-degree-centigrade curve was obtained by extrapolation of the intercepts of the other curves. The rates of oxygen absorption of figure 4 are obtained by taking the average at points on the curve for a 20-hour period, 10 hours on each side of the point in question. The data of figure 4 are not to be considered highly accurate, but are approximate values which may be used in estimating oxygen absorption as a function of oxygen concentration. This transformer was operated until the average oxygen consumption could be determined which would produce a definite acidity. This amounts to 0.4 cubic foot of oxygen per gallon of oil for one milligram KOH acidity.

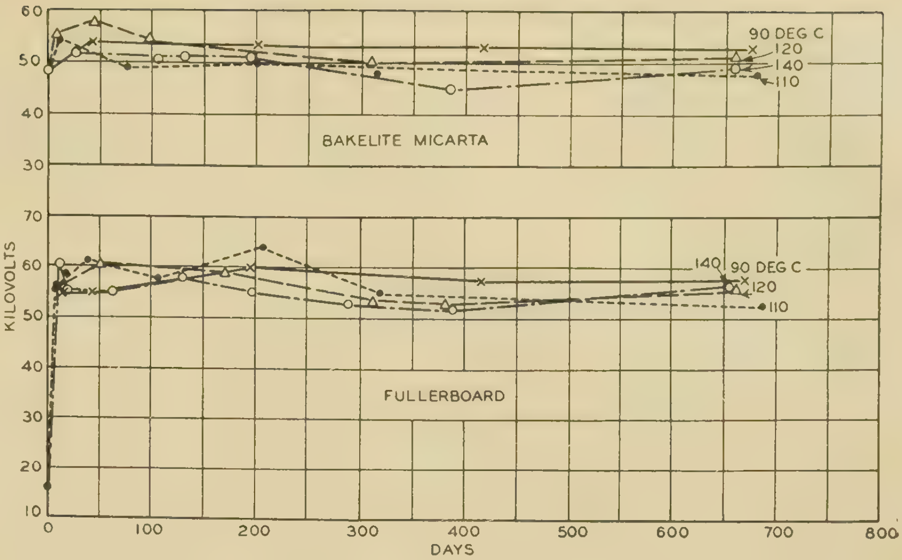


Figure 5. Breakdown tests

An interesting calculation can be made by assuming the oxygen absorption in the larger transformers takes place chiefly at the oil surface or is proportional to the area. It is recognized that much oxygen absorption takes place in the oil condensed on the tank walls, but in general these will be proportional to the oil area. The hourly rate in the large transformer was 0.138 cubic foot per square foot per six-day week or 0.0009 cubic foot per square foot per hour. Calculating the rates on the vertical axis of figure 4 in terms of cubic feet per square foot per hour it is found that the intercept of 0.0009 from this axis on the 85-degree curve is at about six per cent oxygen concentration. The conclusion is that a six per cent oxygen concentration continuously will give a one milligram KOH acidity in such a transformer in 16 or 17 months. A one per cent concentration at the same temperature would give an absorption of 0.000168 cubic foot per square foot per hour. This will require 5.4 times as long or about 86 months to give the one milligram KOH in the same transformer. The ratio of oil volume to surface determines the rate at which acidity builds up in the oil for any otherwise constant condition.

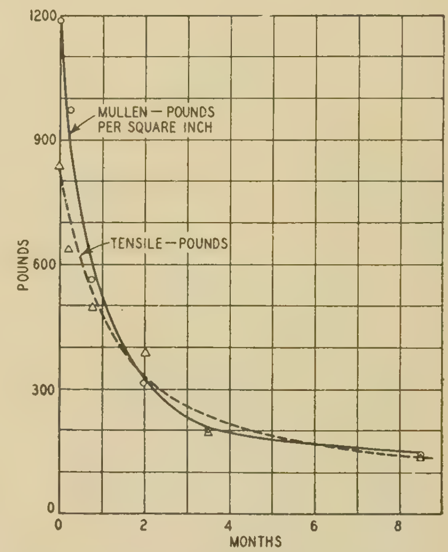
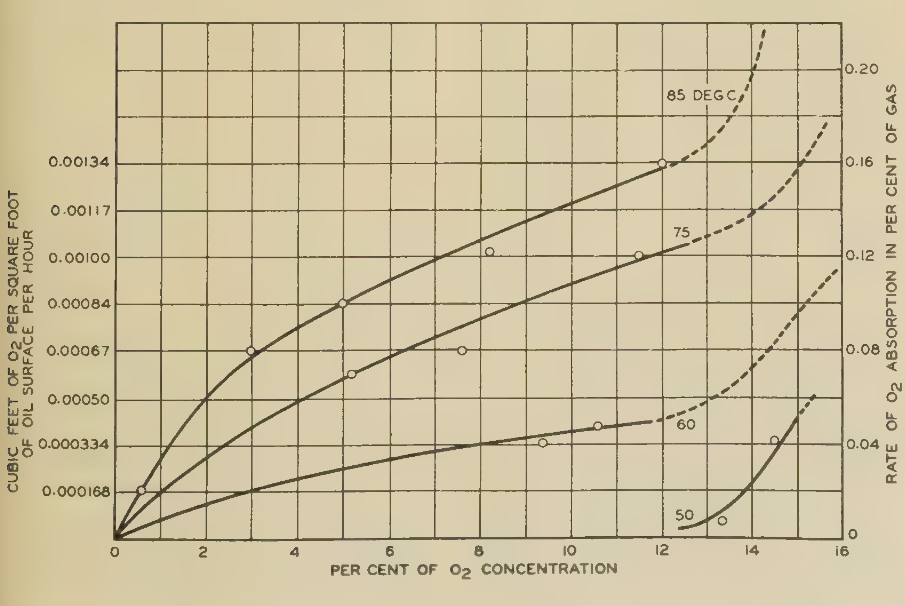


Figure 6. One-eighth-inch fullerboard in oil at 95 degrees centigrade—in air

Figure 4. Rate of O_2 absorption



Life Tests on Cellulose

Cellulose, like oil, is subject to oxidation. Unlike oil, it shows deterioration

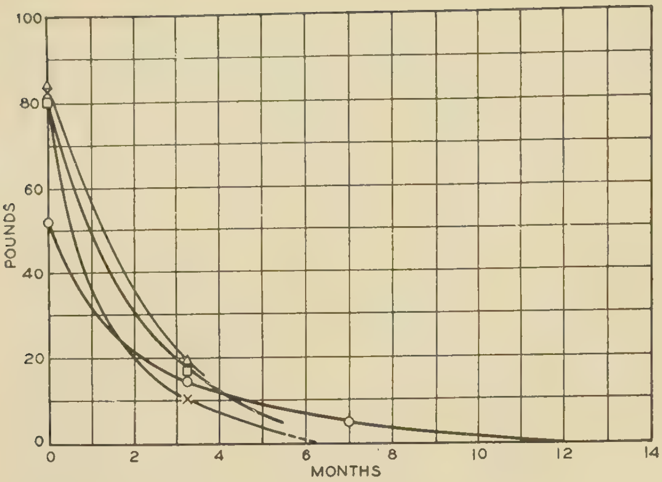


Figure 7. Aged in air (125 degrees centigrade) tensile tests

○—Untreated cotton tape
×—Yellow varnished tape
□—Number 3 varnished tape—one dip
Δ—Number 3 varnished tape—two dips

from temperature alone and at temperatures which may easily exist in transformers. Further, rapid deterioration sets in at rather fixed temperatures. This is due to the removal of what is generally called chemically combined water which, if carried to the limit, leaves only carbon. If cellulose is heated in a perfectly tight transformer to such a deteriorating temperature, this water of decomposition becomes a problem. Some measure of the deterioration might be obtained by the amount of this water given off, but in the end such data would have to be correlated with the properties of cellulose which are important to its use; namely, its mechanical and electrical properties.

In the present study, some impregnated cellulose materials have been included as well as untreated cellulose. The samples were immersed in oil, and the combination exposed to various temperatures and conditions as shown. The data are recorded in figures 5 to 12. Some samples were heated in contact with oil in air while a special large group was tested in oil in a nitrogen atmosphere, by placing them in small containers which in turn were in large, gas-tight tanks. The nitrogen atmosphere was obtained by a small constant flow from a tank, the oxygen content kept to a low value of the order of 0.1 per cent. The tank at 110 degrees centigrade gave erratic results due to lack of temperature and oxygen control early in the test, but some of the data are shown.

Several mechanical tests were used on the insulations with the most emphasis on the tensile strength. The Mullen test is a bursting test as used on fibrous materials for containers. Some attempt was made at embrittlement measurement and it is believed more emphasis should have been placed on such tests, particularly on treated fabrics. The compression test on shellac Micarta tubes (figure 10) demonstrated this to some extent. Electrical breakdown and resistance constituted the electrical tests.

The conclusion from this set of life tests is demonstrated by a few sample data. Electrically, both treated and untreated cellulose maintain their initial characteristics and may even improve. This is true up to 140 degrees centigrade in the inert-gas tests. By the mechanical tests, however, the untreated cellulose is shown to deteriorate rapidly above 100 degrees centigrade. The deterioration is rapid at 95 degrees centigrade with the oil exposed to the air.

A further example of this mechanical deterioration is found in figure 12 where paper tape is tested at 90 degrees, 120 degrees, and 140 degrees centigrade in a nonoxidizing insulating liquid. The tests were made in sealed containers. Figure 10 gives data on varnished tape under inert gas conditions and is to be contrasted with the 125-degree test in air, figure 7. Figure 11 demonstrates the heat resistance of the varnish and possibly that varnish retards the cellulose de-

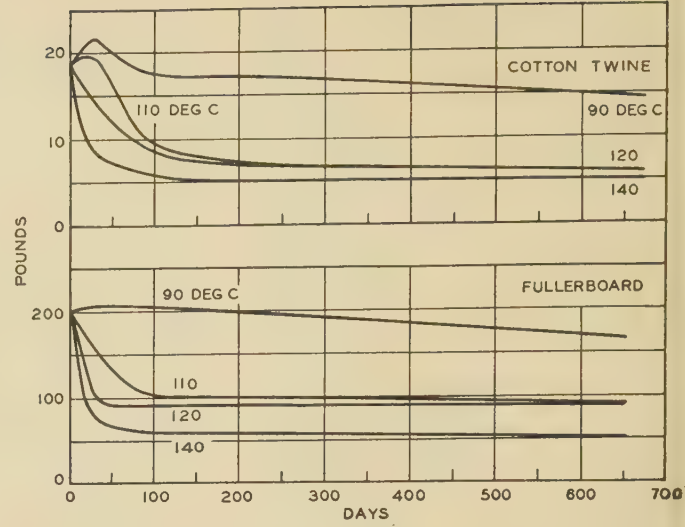


Figure 8. Tensile strength, aged in oil—N_2 atmosphere

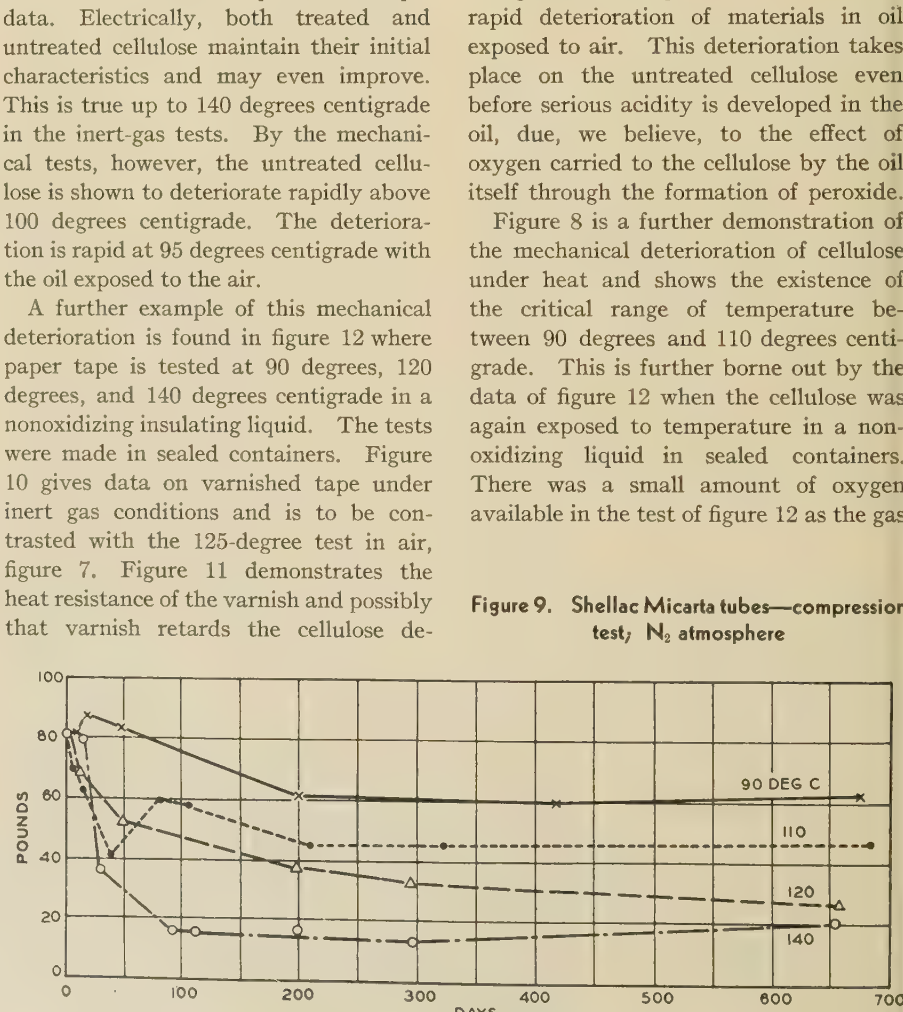
terioration under heat. At any rate, the combination retains its mechanical strength.

Figure 5 and figure 11 demonstrate the type of results on electrical test; namely, that electrically, the insulations retain their properties and even improve.

Figure 6 and figure 7 show the very rapid deterioration of materials in oil exposed to air. This deterioration takes place on the untreated cellulose even before serious acidity is developed in the oil, due, we believe, to the effect of oxygen carried to the cellulose by the oil itself through the formation of peroxide.

Figure 8 is a further demonstration of the mechanical deterioration of cellulose under heat and shows the existence of the critical range of temperature between 90 degrees and 110 degrees centigrade. This is further borne out by the data of figure 12 when the cellulose was again exposed to temperature in a nonoxidizing liquid in sealed containers. There was a small amount of oxygen available in the test of figure 12 as the gas

Figure 9. Shellac Micarta tubes—compression test; N_2 atmosphere



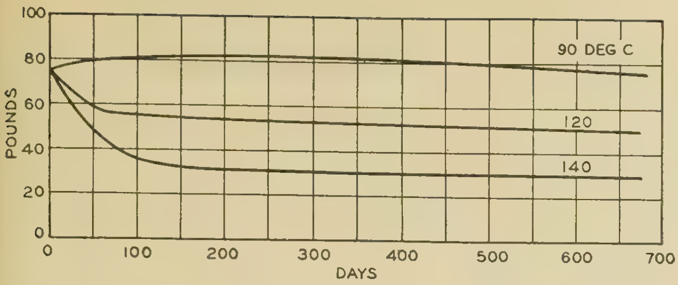


Figure 10. Yellow treated tape—tensile strength; N_2 atmosphere—in oil

above the liquid was air in about the same ratio to oil volume as is found in a sealed transformer.

The data of figure 9 obtained by the force to collapse shellac-paper tubes and also some bending tests in which strips of treated materials were bent to rupture, showed definitely the hardening and embrittlement of impregnating material.

The conclusions from these tests on cellulose and impregnating materials when these tests are combined with experience at lower temperatures, can be condensed into a rather general statement; namely, that up to approximately 90 degrees centigrade, the life of such materials is satisfactory. Above that temperature, mechanical deterioration on bare cellulose becomes rapid, but impregnations protect cellulose to some extent at the elevated temperatures. It is also to be concluded that oxygen restriction is very important to cellulose as well as to oil, and that, while heat alone will mechanically deteriorate the solid insulations at 100 degrees centigrade and above, the presence of oxygen produces deterioration of both oil and cellulose below this temperature.

The method by which oxidized oil affects cellulose is not well understood. The usual impression is that the organic acids are the cause of deterioration and no doubt they have some effect. Stager[4] and Frances and Garrett[5] have discussed this and oil oxidation in more detail, but it now appears the earlier stages of oil oxidation are the more effective.

Predicting the life of a transformer on the basis of the rate of mechanical deterioration is somewhat difficult. However, the insulation is usually used in compression and not for mechanical binding and experience shows that transformers continue to operate after the insulation has reached the very serious apparent deterioration of one-fourth or less than one-fourth of its initial value. This last statement is true for even the best varnished insulations in which the varnish is largely a surface coating.

The results on cotton twine and fullerboard in figure 8 where the oil was exposed to a nitrogen atmosphere, show that untreated cellulose seems to reach a minimum strength of about 25 per cent at 140 degrees centigrade. Further, the decrease took place largely in 100 days. It has since been suspected that the residual oxygen absorbed in the oil may have played a part in this early deterioration, but that is yet to be proved. Our conclusion would be that several hundred hours of operation at 120 degrees to 140 degrees could be permitted for the conditions of an oxygen-free space which may be sufficient to take care of overload periods. Figure 12 gives somewhat the same type of results for the fireproof liquids except that the deterioration was somewhat more. There, however, a small amount of oxygen was present. The leveling off of the mechanical deterioration is largely a dehydration effect in the nitrogen-atmosphere tests.

The extrapolating of such data on solid insulation would seem questionable since it is believed conditions and rates of deterioration change from time to time. It is also uncertain what limits should be reached in an extrapolation of mechanical deterioration. It is the author's opinion that a very large number of transformers in service for a period of years contain fibrous insulation with mechanical strength reduced to 50 per cent of the initial value and in some cases, less than 25 per cent, but function satisfactorily. Some of the insulation from the tests at 140 degrees centigrade in oil with an inert atmosphere would no doubt operate satisfactorily. Such insulation retains good electrical properties. The intrusion of moisture is not to be considered a deterioration, but rather a contamination.

Summary

The experimental data given attempt to show two types of deterioration of cellulose or other organic insulations in transformers. Below 90 or 95 degrees centigrade, such solid insulations would have a very long life under an inert atmosphere.

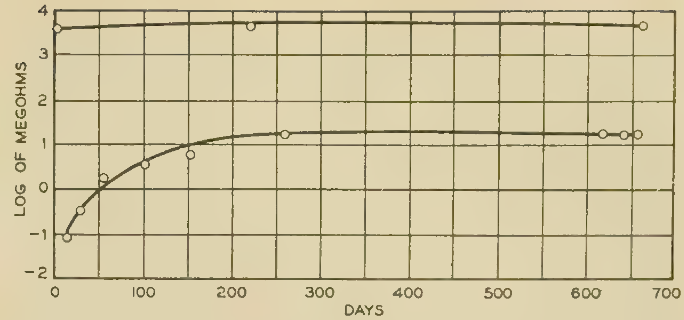


Figure 11. Number 1 varnish cloth—110 degrees centigrade; N_2 atmosphere

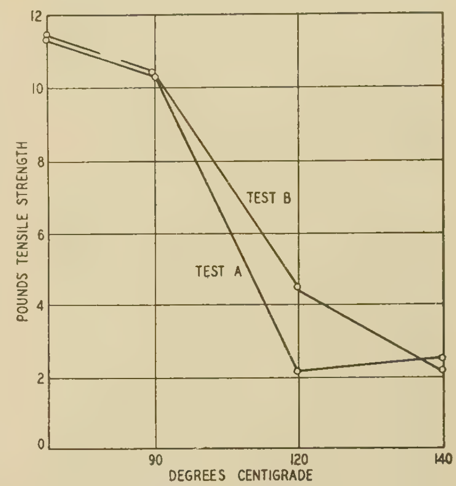


Figure 12. Ninety-six-day tests $2^1/_2$-mil paper tape in nonoxidizing liquid

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5. Frances and Garrett, *Journal* Inst. Petroleum Tech., August 1938, page 435.

Discussion

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): I shall confine my remarks to the life tests on cellulose. The author has reported some very interesting results, the most important of which are (1) that if oxygen is not present cellulose will undergo very little deterioration either mechanically or electrically up to approximately 90 degrees centigrade or 95 degrees centigrade, and (2) that it retains a fair degree of its mechanical strength for many months even at 140 degrees centigrade. The peculiar thing about the results of Doctor Hill's tests is that after approximately 100 days there is apparently no further weakening of the insulation up to 650 days even at 140 degrees centigrade. This is difficult to understand, because we have been led to

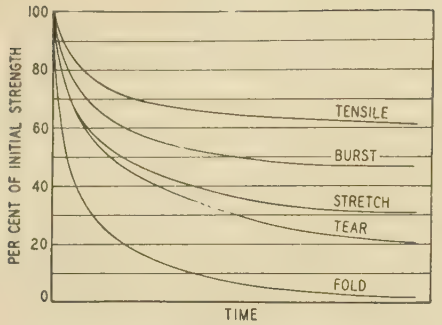

Figure 1. Mechanical deterioration of class A insulations as indicated by tensile, burst, stretch, tear, and folding strengths

believe that all class *A* insulations deteriorate with both time and temperature.

As I see it, there are two phases of this old question of aging of insulation that need further consideration.

The first question is, what causes insulations to age? Just to say that it is due mostly to oxidation does not go far enough. I recently showed, in my discussion of the paper on "Loading Transformers by Copper Temperature," by Messrs. Putman and Dann, the results of aging tests made in oil (1) with nitrogen atmosphere and (2) exposed to air, with practically no difference in the rate of deterioration, which was fairly rapid under both conditions. I do not feel that the mechanism of aging has ever been satisfactorily explained.

Then second, having determined the real mechanism of insulation deterioration, the next problem is to adapt the condition showing the least aging effects to apparatus under service conditions.

If, as shown in this paper, by merely keeping oxygen away from class *A* insulation its temperature can be increased to say 120 or 140 degrees centigrade, it might change materially our present practice of rating and loading apparatus. I do not believe that we are in a position yet to make any radical changes in temperature limits for class *A* insulations, and I believe that Doctor Hill will agree with me on this point. We need further confirmation of these new and very interesting results.

I note that the author's findings on two points agree with what I have found in the past, namely (1) that dielectric strength is no criterion by which to judge the degree of mechanical aging of insulation and (2) that the best method of measuring the amount of mechanical deterioration appears to be in comparing its tensile strength. My experience is that the effect of aging on mechanical strength is least upon the tensile strength. The order of magnitude of the effect ranges from tensile, to burst, to stretch, to tear, and is greatest for the folding test. This takes into account tests made on the principal kinds of transformer insulations including varnished and unvarnished papers, cloths, and pressboards. The wide variations obtained by these different methods of testing are shown in figure 1 which shows that folding tests indicate a very short life as compared with the life as indicated by tensile tests. It might appear that we should use the method which shows the greatest amount of deterioration—folding test. Experience, however, indicates that tensile strength comes more nearly representing the actual life of insulation as used in apparatus.

The question of the kind of test that should be used to determine the life of insulation in apparatus is only one of the many problems confronting an investigator. Generally speaking, no two investigators use the same methods and the results are so contradictory that no definite conclusions can be drawn.

The problem of investigating the aging of insulation is so important to the industry that I would like to suggest that the Institute set up the proper technical committee whose duty would be to correlate the work on the aging of insulation, and recommend standard methods of carrying on the work.

F. M. Clark (General Electric Company, Pittsfield, Mass.): The paper by Doctor Hill constitutes another important contribution to the problem of dielectric deterioration. Data in this field are rapidly accumulating and despite the wide differences noted at times, a clearer understanding is being obtained with respect to those factors which are important for the successful operation of commercial machines. Unfortunately, the problem of setting up temperature limits covering commercial operation of oil or cellulose or oil-treated insulation involves carefully controlled studies over long periods of time. I am, however, inclined to disagree with Doctor Hill when he states that data obtained in commercial equipment is somewhat more convincing than laboratory tests. Oxidation and pyrolysis tests are so affected by factors which are controlled only with difficulty even when known to be present that data obtained on large scale tests can be accepted as reliable only when supported and explained by accurately determined laboratory results. This is especially true for tests on mineral oil and cellulose.

Oxidation is, of course, an important factor in the successful operation of electrical apparatus and is an ever present threat which must be included in the life evaluation of organic insulation despite the varied methods which have been suggested for its elimination. But oxidation is not the only threat to the successful operation of cellulosic insulation at the higher temperatures of operation. This has been pointed out by Doctor Hill but I believe that the effect of nonoxidizing high-temperature exposure warrants greater consideration. Doctor Hill states that "heat alone will mechanically deteriorate the solid insulation at 100 degrees centigrade and above." This is true. In a paper which I presented before the Institute a few years ago entitled "The Pyrochemical Behavior of Cellulosic Insulation" (ELECTRICAL ENGINEERING, volume 54, October 1935, pages 1088–94) it was demonstrated that cellulosic insulation when exposed to high temperature bears the stamp of that exposure even though the high-temperature treatment be carried out under nonoxidizing conditions. The effects of this exposure fundamentally are chemical, and result in the chemical degradation of the cellulose, gas evolution, and deteriorated dielectric properties. These dielectric effects are especially noticeable for tests carried out on what are usually considered the highest type of dielectric materials such as cable and papers. I suggest that the reason the breakdown tests presented by Doctor Hill in figure 5 do not show such deterioration is that the breakdown examined is a function of the preceding corona discharge. This corona discharge usually occurs in tests such as those cited by Doctor Hill and its effect very largely determines the breakdown value, masking other factors which may be present.

Doctor Hill differentiates between the thermal aging of insulation at temperatures above 100 degrees centigrade and the oxidation aging of cellulose below 100 degrees centigrade. The part played by mineral oil in the aging of cellulosic insulation at 100 degrees centigrade and below has been a debatable question. Doctor Hill apparently agrees with those who claim that acids formed in the oil as a result of oxidation are factors contributing to the mechanical weakening of the insulation. Doctor Hill goes further and suggests that the "deterioration takes place on the untreated cellulose even before serious acidity is developed in the oil, due, we believe, to the effect of oxygen carried to the cellulose by the oil itself through the formation of peroxide." This is important if true. From the data submitted by Doctor Hill it is impossible to decide the validity of this assertion. That it may be true is indicated by a comparison of figures 6 and 12. Figure 12 concerns itself with paper tape immersed in a nonoxidizing and noninflammable liquid in a sealed container in contact with air, and figure 6 describes tests on fullerboard immersed in oil presumably with free exposure to air. However, Doctor Hill presents no data concerning tests on paper tape under oil similar to the tests described in figure 12. Presumably the amount of air in the sealed container of figure 12 constituted about 10 per cent to 15 per cent of the total volume of the container so that enough oxygen was present to give some oxidation effects if oxidation were an important factor with cellulose immersed in noninflammable type of insulating liquid. In such a material peroxide effects are definitely absent. The oxygen cannot be used up by the oxidation of the liquid which is chemically inert. Therefore, the effects of cellulose oxidation can be obtained only by the migration of the oxygen through the inert noninflammable liquid. This might be expected to retard the mechanical deterioration due to oxidation. Figure 12 confirms this expectation and shows that the paper tape under the noninflammable liquid shows no substantial change in mechanical strength after 96 days at 95 degrees centigrade. Figure 6 shows that the mechanical strength of the fullerboard in oil fell about 75 per cent during the same interval. These data may well indicate that the noninflammable liquids because of their chemical stability and nonoxidation characteristics effectively reduce the tendency of cellulosic insulation to deteriorate mechanically at temperatures of operation below 100 degrees centigrade where oxygen-free atmospheres have been shown by Doctor Hill to be of value for mineral-oil-immersed insulation.

There can be but little doubt that oil acids must not be ignored in the evaluation of the mechanical characteristics of oil-immersed cellulose. Mineral oil when oxidized pro-

duces oil-soluble products which are capable of absorbing alkali. These materials are usually classed as oil acids. Total oil acidity as illustrated by Doctor Hill in figure 1 does not appear to me to be of major importance. What does appear of importance is the formation of water-soluble organic acids. These are present to an extent dependent on the type of oil base and on its refining treatment. With the highly refined (white) oils the water-soluble corrosive acids predominate. With a carefully selected oil whose refining treatment has been properly supervised these water-soluble corrosive acids are present in only negligible amounts. The variation in the formation of these water-soluble corrosive acids may account for the wide differences which are at times reported in the literature covering the mechanical aging of oil-immersed cellulose.

Acetic acid is one of the corrosive organic acids evolved by the oxidation of highly refined white oils. It is this type of acid which is formed by the exposure of cellulose to high temperatures even in the absence of oxygen. Cellulose evolves a variety of degradation products whose formation begins slowly at relatively low temperature. These are in general poor dielectrics and of strong chemical reactivity. The presence of these products retained in contact with the cellulose accelerates further deterioration, a weakening in mechanical strength, and a decrease in dielectric efficiency. For high-temperature operation these effects appear to predominate. For lower temperature operation (80 degrees centigrade to 90 degrees centigrade) the most important factor affecting the mechanical life of cellulose based on the data of Doctor Hill appears to be one of oxidation. This apparently can be reduced and possibly eliminated by the use of nitrogen atmospheres to replace air or oxygen or by the use of a nonoxidizing, noninflammable liquid.

H. V. Putman (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): The thing which strikes me as most significant about Doctor Hill's results is the extremely wide range in the effectiveness with which the oil and insulation are protected against deterioration in different types of transformers. Using Doctor Hill's data, Mr. Vogel has shown that the life of the oil in transformers under nitrogen may be 50 times as long as with open-air breathing.

If we examine Doctor Hill's figures 7 and 10, we see that yellow treated tape in oil under air deteriorates as much in a month as it does in oil under nitrogen in two years.

And yet our present standards establish the same maximum temperature limits and short-time overload capacities for all types of transformers. Obviously those having effective means for protection of oil and insulation can stand higher temperatures and more severe overloads than those not having such means.

I am not prepared to say that we should increase the standard temperature rise of 55 degrees centigrade for transformers. Perhaps we should outlaw open-air breathing. Mr. Hellmund has pointed out that perhaps the reason European manufacturers are successful with a higher temperature rise is because all their transformers large and small are protected by some kind of a conservator.

It does appear in order to suggest that the AIEE transformer subcommittee undertake a review of our present rules affecting maximum temperatures and overloads with a view to bringing them into harmony with the engineering facts established in Doctor Hill's paper.

H. C. Louis (Consolidated Gas Electric Light and Power Company of Baltimore, Md.): The paper by C. F. Hill, presents the results of some very elaborate laboratory tests showing the effects of temperature and aging on the electrical and mechanical qualities of insulation. The results add much information useful in the design, rating, and operation of electrical apparatus as related to insulation. They contribute fundamental data of value in the important and much debated subject under consideration, the allowable temperature limits of insulation, and whether those in existing standards should be modified.

We note with particular interest that in these carefully controlled laboratory tests, in some cases of definitely marked deterioration of mechanical characteristics of insulation the electrical characteristics did not deteriorate correspondingly, and continued to be apparently fairly good. This checks with a condition sometimes experienced in actual operation in cases where apparatus seems to be able to operate satisfactorily and reveals no weaknesses on electrical tests, although inspection may show the insulation to be in very questionable mechanical condition.

This question of determining the condition and expected additional life of insulation of used apparatus is one of practical operating concern, involving reliability of service and economics. Development and investigations of field methods for checking the condition of insulation by special tests and inspections should continue to be encouraged.

The correlation of the data from the paper under discussion with others relating to the same subject, as recommended by Mr. Montsinger, should therefore serve not only the primary purpose of the question of temperature limits and ratings, but should also contribute useful information in the problem of judging by tests and inspection the condition of insulation of apparatus in use, and the possible additional life of same.

R. E. Hellmund (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): As already briefly stated by Mr. Putman, the data presented by Doctor Hill in his paper and other calculations given by Mr. Vogel in his written discussion seem to explain a controversy of long standing with the International Electrotechnical Commission regarding permissible temperature ratings of transformers. The European countries wish to standardize on temperature-rises somewhat higher than those considered safe by the American representatives. The early assumption that the European practice might be less conservative can hardly be considered correct, because the scarcity and high price of oil in Europe make it necessary for them to be more economical in the use of oil than it is in this country. A slight difference in climatic conditions might, of course, have some influence on their service experience. However, it seems that on account of the greater need for preservation of materials, expansion tanks have been used in Europe to a great extent for transformers of even the smallest sizes. In contrast to this, in this country the distribution transformer has been used until fairly recently without such tanks and with the surface of the oil freely exposed to air and oxygen. It seems from these newly available data that they may have obtained service results as good as or even better than those we have obtained in the past, even though their rating standards include higher temperature-rises. Although it may be premature for us to change our rating standards at this time, it seems advisable in our international contacts to recognize these differences and to make some concessions in connection with the work of the IEC.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): This paper adds considerably to our knowledge of oil-impregnated insulation. The effects of temperature are shown to vary appreciably depending on the conditions of test and test procedure. Nevertheless the paper brings out again the point that when class *A* insulation is operated continuously at temperatures of 90 or 95 degrees centigrade or higher the rate of deterioration increases fairly rapidly. In other words, a short life will obtain when the insulation is operated continuously at the present "hot spot" temperature limit of 105 degrees centigrade of the AIEE rules.

Doctor Hill's data show that when testing paper under oil which is exposed to air the oxidation products of the oil will affect the strength of the paper. This test procedure is satisfactory, I presume, if it simulates the service condition. For some equipment, such as capacitors and cable, the oxygen, however, is pretty well removed from the insulation in manufacture and kept so during operation.

In connection with his figure 12 showing the decrease in tensile strength with temperature, it seems to me that the criterion of tearing strength is most valuable to use in such tests. This test gives a large range in results of tests of deteriorated material and the results seem to be most consistent. Probably the folding endurance test, as developed in the research at Massachusetts Institute of Technology about 15 years ago, is too much influenced by moisture, both free and combined, to be reliable.

F. J. Vogel (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): Doctor Hill's paper should be of considerable interest to all users of transformers because it gives basic data which can be used to estimate the life of oil and insulation under various conditions of operation. It shows how extremely important it is to protect transformer oil against oxidation, particularly when the oil is operated at high temperatures. It shows that even small amounts of oxygen continuously admitted to the transformer tank over long periods of

time can seriously shorten the life of the transformer oil. Consequently, it is important that gasketing be done in the best possible and most permanent manner. A positive pressure of nitrogen ranging from one-half pound upward is further assurance against the possibility of oxygen or air entering the transformer tank.

Doctor Hill has shown that deterioration of insulation is a function both of temperature and oxidation, while the deterioration of the oil is primarily a function of oxidation, which, fortunately, can be eliminated almost completely by the use of nitrogen above the oil level.

For example, Doctor Hill shows that 0.4 cubic foot of oxygen per gallon of oil will produce an acidity of one milligram of KOH. Sludging is usually considered to begin when the acidity reaches 0.5 milligram of KOH, which would correspond to 0.2 cubic foot of oxygen per gallon of oil. For comparative purposes it is interesting to calculate the time required to produce this amount of acidity in the oil under various conditions of breathing in a typical transformer. As an example, we have used a 2,500-kva unit containing 680 gallons of oil, having an 88-inch oil level, with 10-inch gas space and 16.4 square feet of oil surface. This transformer would require 136 cubic feet of oxygen to produce 0.5 milligram of KOH acidity in the oil. We know from experience that a transformer of this size would use from 1 to $1^1/_2$ cylinders of nitrogen per year, or, roughly, 275 cubic feet. This gas is purchased under specification limiting impurities to 0.5 per cent, and for the purposes of calculation it may be assumed that the impurities are entirely oxygen—or 1.37 cubic feet. It may further be assumed, for the purpose of calculation, that the transformer is opened up once per year for inspection, and when closed up the gas space is blown out to five-per cent oxygen concentration, which would leave an additional 0.68 cubic foot of oxygen in the gas space annually. The total amount of oxygen in the gas space available for oxidation of the oil would therefore be 2.05 cubic feet per year, which would require 67 years before any sludging would appear—and most important, this would not be dependent upon the temperature of the oil.

If the transformer tank were bolted up tight with no breathing but were opened once a year for inspection and the gas space filled with air upon closure, 50 years would be required for sludge to appear.

In making calculations for various types of breathers, it is necessary to assume some daily load cycle and also to establish an average oil temperature. Seventy-five degrees has been assumed for the oil temperature with a temperature fluctuation of 20 degrees daily. Under this condition the transformer would breathe in approximately 612 cubic feet of air per year containing approximately 123 cubic feet of oxygen.

Let us assume, at this point, that the transformer is equipped with a single breather which takes in air when the transformer cools and breathes it out during the heating-up part of the cycle. Very approximately we can say that the amount of air breathed in equals the amount of air breathed out. This is not quite true due to the fact that there is some gas absorption in the oil. More exactly, we can say that the amount of oxygen breathed in is equal to the amount of oxygen breathed out plus the amount of oxygen absorbed by oxidation in the oil. To fulfill this latter equation, it is necessary to assume an average oxygen concentration and refer to figure 4 of Doctor Hill's paper to see if the amount absorbed plus the amount breathed out will equal that taken in. Assuming approximately $5^1/_2$-per cent average oxygen concentration, 34 cubic feet of oxygen would be discharged in the out breathing. From figure 4 the rate of oxygen absorbed per square foot of oil surface per year would be 0.00062. At this same concentration the amount absorbed in a year would be 0.00062 $\times$ 16.4 $\times$ 24 $\times$ 365 or 89 cubic feet. The conditions of the equation regarding oxygen are fulfilled, since 123 cubic feet breathed in equals 34 cubic feet breathed out plus 89 cubic feet absorbed. Since 136 cubic feet of oxygen are required for the oil to reach 0.5 milligram of KOH acidity, and 89 cubic feet would be absorbed annually under the conditions described, it would only take about a year or a year and a half for the oil in a transformer with a single breather to reach a condition where it would begin to sludge.

Similar methods of calculation may be used for other breather arrangements.

Occasionally double breathers are used which provide a constant circulation of air through the gas space above the transformer. This arrangement is quite advantageous from the point of view of preventing moisture condensation on the cover. However, it does provide a high oxygen concentration above the gas space with consequent short oil life. Assuming the same average oil temperature (75 degrees centigrade) and only 16 per cent oxygen concentration (which seems conservative) the oil in the transformer should require only 0.6 of a year to reach 0.5 milligram of KOH acidity.

The case of the simple expansion tank can be calculated with less assurance from the data presented by Doctor Hill because the oil in the conservator tank might be somewhat more quiescent than in the main tank and therefore less accessible to oxidation. Also the temperature of the conservator tank varies widely depending on the type of connection to the main tank. However, in an actual heat run the temperature of the oil in the conservator was 58 degrees, when the oil in the main transformer was 75 degrees. Assuming the same daily load cycle as previously and applying the calculation to the surface of the oil in the conservator, which was $7^1/_2$ square feet, it was found that of the 123 cubic feet of oxygen breathed in, 85 were breathed out and 38 absorbed, with the result that 3.6 years would be required before sludging would appear. With types of construction which tend to reduce the oil circulation and the temperature of the oil in the conservator, the time might be considerably longer—perhaps even 6 or 7 years.

The merit of nitrogen protection or tightly sealed tanks is apparent from the above calculations from the standpoint of oil deterioration.

In interpreting the insulation deterioration results which Doctor Hill presents, in terms of actual transformer construction, it should be recognized that the insulation in the transformer, which is subjected to appreciable mechanical stress, is actually at the oil temperature rather than at the copper temperature. In fact, it is only the insulation covering the copper conductor itself which reaches the maximum copper temperature, and here a deterioration in tensile strength of 50 to 75 per cent is probably not serious once the insulation is in place in the transformer. Doctor Hill's data on insulation deterioration in nitrogen indicate that the deterioration due to temperature takes place relatively quickly—that is, in 100 to 150 days. After that there appears to be little further deterioration, and the loss in tensile strength even at temperatures as high as 120 degrees would not appear to be a serious matter. This is particularly true since most of the insulation is used in compression rather than in tension, and the decrease in tensile strength is probably not accompanied by a commensurate decrease in compression strength.

It can be concluded from Doctor Hill's data that the maximum safe temperature limits, particularly for short periods of time, can be considerably higher in types of transformers having effective protection of the oil and insulation against oxidation.

The proposed recommendations of the American Standards Association for short-time overload capacity of transformers appear to be extremely conservative when applied to transformers having protection against oxidation. With such transformers, continuous operation at rated load, 55 degrees rise, and at 40 degrees ambient, is possible without measurable deterioration.

Charles F. Hill: Mr. Putman and Mr. Hellmund have called attention to European practice of higher temperatures in distribution transformers than permitted in America, and the fact that my results explain and justify the argument for this European practice. As Mr. Putman points out, it is illogical to use devices to keep out oxygen and then limit the transformer to the same temperature as a free-breathing unit.

Mr. Montsinger points out the apparent flattening off of the deterioration rate of cellulose in my results, after a relatively few months. The flat part of the curve could have been given a slight slope and still fit the data, but the rapid initial effects were rather striking. I suspect some initial oxidation due to residual oxygen in the oil with more frequent opening of the apparatus could have distorted the early results in this respect. The important point is that the mechanical deterioration at the end of two years is not nearly so great as normally expected. The shape of the curves points out the difference in conclusions to be reached, depending upon the length of the life test as emphasized by Smith and Scott in their paper.

As to the rate of deterioration and the "8 degree" rule or "10 degree" rule now under consideration, I believe this is too conservative for this case, but this subject is analyzed in more detail in my discussion of the paper by Smith and Scott (page 444).

My results emphasize again the accepted conclusion that deterioration of the organic materials (solids) is primarily mechanical and not electrical. The choice of tests to measure this deterioration is somewhat a problem, but we have concluded tensile tests are most dependable.

Referring to Mr. Halperin's comment, it is true as he points out that these results as well as previous experiments and experience show a critical temperature for cellulose around 100 degrees centigrade, but to get 100 per cent deterioration, even at higher temperatures, requires a long time. Our results also show, as he mentions, that oxygen has a very marked effect when both high temperature and oxygen are applied to oil-immersed cellulose.

Mr. Halperin has also raised the question of type of test and suggested a tearing test. I believe embrittlement measurements for this case would be more in order. We have had some degree of success with these.

I agree in general with F. M. Clark's statements concerning the relative value of controlled laboratory tests and "commercial" tests. The data on oils in my paper, figures 2 to 4, inclusive, are more nearly laboratory experiments than commercial in that conditions were rather closely controlled.

It is not clear to me just what Mr. Clark's explanation of the apparent lack of electrical deterioration may be. The oil-soaked fullerboard remains good electrically, as also did the oil in the inert gas tests.

Mr. Clark has also discussed the mechanisms by which oil or oxidized oil deteriorates cellulose. It was not the intention to add data to the paper to prove a theory of these mechanisms, but from other results we do believe peroxide stages of oil oxidation are a serious factor. Concerning the ability of oil to carry oxygen to cellulose, we believe that is generally accepted and it is probable that in the presence of an oxygen atmosphere, the nonoxidizing fireproof liquids may show less effect upon cellulose at 80–85 degrees centigrade than does oil. As Mr. Clark states, also, the lighter water-soluble acids formed in oil oxidation are the more active.

Our experiments were so selected as to show the deterioration effects and their degree within the various temperature ranges and so far as I can see from Mr. Clark's discussion, he agrees in a general way with our results. We recognize, of course, that some of the mechanisms of deterioration are not as yet explained.

Mr. Vogel has used the oil data to determine the life of a transformer oil under various conditions of operation, that is, under various amounts of available oxygen for oil deterioration. I believe our results will be of considerable value to the user of transformers in determining what he should demand in the way of protection against oil oxidation.

The data on oil and cellulose deterioration both should be of value to the operating engineer in determining what to do with transformers in service. Mr. Louis points out in his discussion that the operating engineer needs such information. I believe, as Mr. Louis suggests, that the data given will be found of considerable value. No doubt more experiments may be necessary to give a complete and accurate picture and I hope operating engineers will attempt to utilize the data enough to evaluate the results.

So far as temperature limits and standards are concerned, the paper raises some very important questions, but these have been pointed out by Mr. Putman and need no further discussion.

Capacitors and Automatic Boosters for Economical Correction of Voltage on Distribution Circuits

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Synopsis: Analysis of operating characteristics shows that fixed shunt capacitors may be used to provide voltage correction up to approximately three per cent on a typical four-kv distribution circuit. Installation of capacitors up to this limit, or to unity power factor on the feeder if that occurs with less capacity, generally gives voltage improvement at minimum cost.

Additional correction can be secured with a set of automatic voltage boosters beyond the shunt capacitors, except that single-phase taps from near the feeding point should have the other phases extended first to secure lowest cost. This procedure requires only three different devices, a 30-kva single-phase capacitor, a 90- to 180-kva three-phase capacitor, and a 5-kva automatic booster with two 1.33-per-cent steps, to secure appreciable economies in distribution cost.

RECENT years have seen the introduction and promotion of three additional types of equipment to correct abnormal voltage conditions on distribution circuits, namely: (1) the automatic voltage booster, (2) the shunt capacitor, and (3) the series capacitor. Each one functions in a different way to regulate voltage, and has certain other effects, which make direct comparison of price an unfair test of true economic value. This paper undertakes to compare them as they might be applied to the system of the Duquesne Light Company, subject to the voltage limitations and other conditions of that system, and to ascertain which device or combination of devices should be used to maintain the voltage at consumers' service switches within the prescribed limits. Inasmuch as the Duquesne Light system is generally similar to those operating elsewhere in areas of similar population density, it seems probable that the conclusions will be fairly representative.

The typical circuit is three-phase, four-wire, four-kv, regulated at the substation by single-phase induction regulators. By means of line drop compensators, the regulators are made to correct for voltage drop in the main feeder, and it is assumed that they regulate the primary voltage directly at the first tap or transformer (called the "feeding point"). This feeding-point voltage normally varies between 102.0 per cent at light load and 104.0 per cent at peak load, always with a tolerance of ±1.0 per cent for the margin between "raise" and "lower" in the contact-making voltmeters controlling the regulators. These values have been established to give as good voltage as possible as much of the time as possible to as many consumers as possible without sacrificing the economic advantage of raising the feeding-point voltage close to the 105.0 per cent limit during peaked load. With voltage drops at peak load of up to 3.0 per cent in the primary system, 2.5 per cent in distribution transformers, 2.0 per cent in secondaries, and 0.5 per cent in services, the delivered voltages will range from nearly 104.0 ± 1.0 per cent for a consumer fed by a very lightly loaded transformer at the feeding point down to 96.0 ± 1.0 per cent for a secondary consumer at some distance from a heavily-loaded transformer at the end of the primary. This circuit is discussed more fully in the paper "Automatic Boosters on Distribution Circuits"[3] from which figure 1 is taken.

Attempts to benefit by the assumption that consumers near the feeding point always have at least the voltage drop of a 50-per-cent-loaded transformer below the primary voltage at the feeding point during the circuit heavy-load periods, accordingly raising the feeding-point voltage an extra 1.0 per cent to 105.0 ± 1.0 per cent, have not been successful on the Duquesne Light system and have been abandoned because of complaints. The conditions shown in figure 1 represent the maximum voltage drops with which service voltage can be maintained within the established limits, and are typical of a 1,500-kva four-kv open-wire circuit feeding

Paper number 39-63, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 2, 1938; made available for preprinting January 4, 1939.

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3. For all numbered references, see list at end of paper.

three or four square miles of fairly uniform suburban load with an extreme length from the feeding point to the end of the longest primary branch of around two miles.

But load increases both in density and in area, and the tendency is to extend the

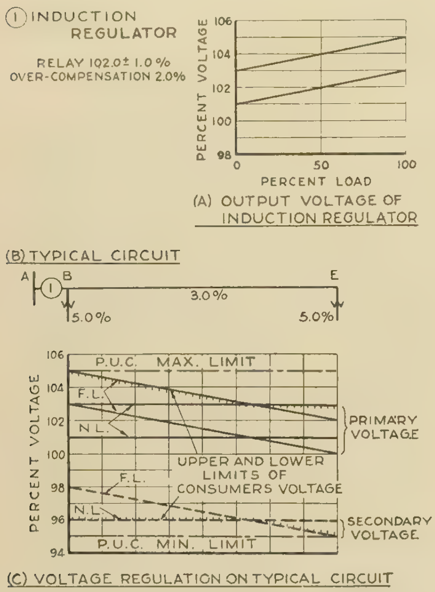

Figure 1. Voltage regulation of typical distribution feeder by induction regulators at substation

long branches and to carry more and more load on the circuit. Immediately the voltage drop increases, and before long it becomes necessary to provide some means to bring the voltage back within the established limits. When the circuit has ample current-carrying capacity, an advanced feeding point is established nearer the center of load and a back feed is built to serve load along the feeder. Sometimes that does not suffice, and a new feeder or substation may be built to share the load. Both methods ordinarily are expensive, and frequently can be delayed for a number of years by the use of voltage corrective devices,[2] sometimes with considerable savings in annual cost.

Automatic Voltage Boosters

About six years ago leading power companies started to use automatic voltage boosters to correct low voltage on long circuits, particularly for rural districts where rather coarse voltage steps were adequate. The size of step is directly reflected in the output voltage, however, and the earlier paper[3] indicated a two-step booster having 2.66 per cent total range as the most economical design for operation within a total primary voltage variation of 5 per cent. The effect of such a booster is shown in figure 2; with a primary voltage drop 89 per cent greater than could be permitted in the original circuit, figure 1*B*, the voltage delivered to consumers is held within the same limits by means of the automatic boosters. Analysis showed that automatic boosters were economically applicable where satisfactory regulation could be secured without boosters only by extensive installation

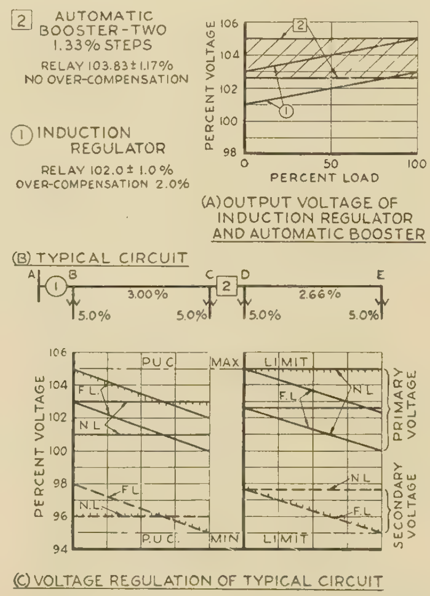

Figure 2. Voltage regulation of typical distribution feeder by induction regulators at substation and automatic boosters on poles

of 1/0 or 4/0 conductors on the three-phase portion of the circuit.

Shunt Capacitors

In 1930 a number of four-kv shunt capacitors were installed by the Duquesne Light Company on circuits in industrial areas to correct for the low power factor then existing. The depression which came even before these installations were complete had the triple effect of reducing the industrial load, making industrial consumers more anxious to benefit by allowances for good power factor, and retarding the expenditures required to serve adequately the continually growing domestic load particularly in areas only recently developed. As early as 1932, capacitors were being moved from industrial circuits to suburban areas to reduce the voltage drop in overextended circuits.

The voltage corrective effect of the shunt capacitor is due to the leading reactive current which, flowing through the circuit from the regulated source at the feeding point to the capacitors out in the load area, produces a rise in voltage equal to the product of the capacitative current and the line reactance. This voltage correction varies with the distance, from zero at the feeding point to full value at the capacitor. Ordinarily the capacitor is permanently in service, and so raises the voltage by the same amount at light load on the circuit as at full load.[4]

The maximum correction which can be secured by use of fixed shunt capacitors can be determined very readily from charts of voltage at the proposed location, the limit being that capacity which raises the voltage during light-load periods to the upper limit of 105.0 per cent. Usually the light-load voltage along a circuit is practically constant along the entire length, substantially equal to the contact-making voltmeter setting of 102.0 ± 1.0 per cent. With these voltage conditions only 2.0 per cent voltage correction can be secured by means of shunt capacitors. More correction can be used, however, by balancing the voltmeter at 101.0 per cent and increasing the line drop compensation, which would permit as much as 3.0 per cent correction by means of shunt capacitors.*

It is seldom possible or desirable to locate the capacitors directly at the end of the circuit, as this would give the maximum voltage correction at one point only and less correction at the ends of other branches of the circuit where full correction is as much needed. Generally they are installed at some such point as the end of the three-phase circuit or a branch point of the three-phase circuit, in either case leaving some drop in the primary laterals beyond the capacitor to the last transformers. A circuit embodying these characteristics is shown as figure 3, which indicates that full advantage has been taken of the permissible variation in voltage and that it is not feasible to correct the primary voltage more than 3.0 per cent with fixed shunt capacitors.

If more than 3.0 per cent correction is desired, then additional capacitors can be installed with a switch controlled by a contact-making voltmeter to cut them into service when the voltage drops below the desired level. With voltage limits at the point of installation of 101.0 per cent to 105.0 per cent, automatic voltage-controlled shunt capacitors could

* Usually the light-load voltage can be lowered somewhat more, which would permit some additional voltage correction by shunt capacitors. It must be borne in mind, however, that a circuit having good phase balance at peak load may have appreciable unbalance at light load, and that this unbalance added to the voltage rise caused by fixed shunt capacitors might cause excessively high voltage on the lightest loaded phase during light load. Accordingly, the limit to voltage correction by fixed capacitors has been set at three per cent.

be applied to give up to 3.0 per cent additional voltage correction. A circuit with both fixed and automatic capacitors could have a total of 9.0 per cent primary drop, of which 6.0 per cent would be corrected by the capacitors. The automatic capacitors would require individual phase control to compensate for neutral shift, and might as well be single-phase installations. Voltage regulation on a typical circuit with both fixed and automatic shunt capacitors is shown in figure 4.

The shunt capacitor raises voltage by drawing capacitative current. Inasmuch as the typical distribution feeder has a peak-load power factor approximately 90 per cent lagging, the shunt capacitor tends not only to improve voltage but also to improve the circuit power factor. This results in lower kilovolt-ampere demand for the same peak load, and releases carrying capacity in the circuit and in all of the supply system. On systems having low power factor this improvement in power factor is most desirable; the system capacity released is available to

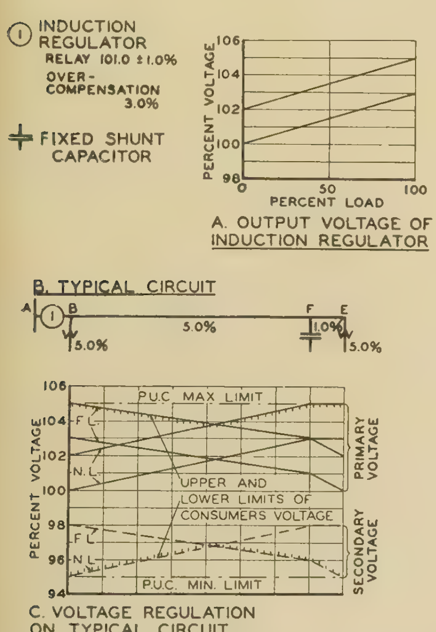

Figure 3. Voltage regulation of typical distribution feeder by induction regulators at substation and fixed shunt capacitors on poles

carry increased load and has the same value per kilovolt-ampere as an addition to the system.[5] When the system power factor already runs higher than the rating of the generators, then the only value creditable to improved power factor is the capacity released in main feeder, substation, and transmission system. The latter is true of the present analysis, and a value of $50.00 per kilovolt-ampere has been assumed for the reduction in demand. With the present price of 2,400-volt capacitors running under $8.00 per capacitative kilovolt-ampere, it is evident that credit for reduction in peak demand might pay a large portion of the cost of capacitors, leaving a rather small charge for the voltage improvement for which they are installed.

For example, a circuit carrying 1,500 kva at 90 per cent power factor might require the installation of 360 kva of shunt capacitors for voltage correction. The capacitors would raise the circuit power factor to 98 per cent and reduce the circuit demand measured at the substation from 1,500 kva to 1,385 kva. This reduction in demand of 115 kva, evaluated at $5,750, is secured from shunt capacitors having a purchase price of approximately $2,700. With the cost of the capacitors more than covered by credit for reduction in demand, the voltage correction is secured free of charge.

Series Capacitors

Capacitors of proper rating installed in series in a circuit also tend to reduce the voltage drop.[1] The effect is computed readily as the product of the capacitative reactance and the reactive component of

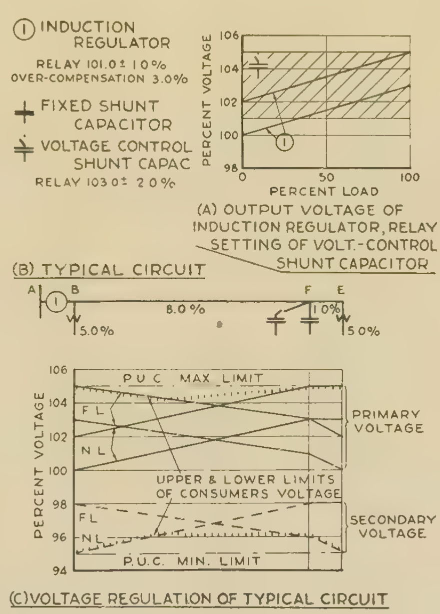

Figure 4. Voltage regulation of typical distribution feeder by induction regulators at substation and shunt capacitors, partially controlled by voltage, on poles

the current flowing through the capacitor. Installed at the point where the primary voltage reaches the lower limit, 101.0 ± 1.0 per cent, it should have such an impedance as would, with the maximum load current flowing through it, raise the voltage 3.0 per cent, or to 104.0 ± 1.0 per cent. As the load decreases, the power factor drops, and the out-of-phase component tends to decrease but little. The voltage boost of the series capacitor, therefore, is nearly as great during light load as during heavy load, and it is necessary to check several different load conditions to avoid excessively high voltages on the output side. For present purposes, however, it is assumed that the capacitor selected to give 3.0 per cent rise at peak load is satisfactory. The voltage regulation of the typical circuit with this series capacitor is shown in figure 5.

Economic Comparison

There are now five different methods for correcting the voltage regulation of a circuit, namely:

1. Rebuild for greater current-carrying capacity, by changing single-phase to three-phase, replacing present conductors with larger conductors, or building a new circuit to divide the load.

2. Extend the feeding point farther into the load area, back feeding the load along the main feeder and regulating voltage at the new feeding point by increased use of the feeder regulators at the substation.

3. Correct for excessive voltage drop in the existing circuit by installing automatic voltage boosters at the proper points.

4. Correct for excessive voltage drop in the existing circuit by installing shunt capacitors near the ends of the branches. Where fixed capacitors alone do not suffice, additional correction can be secured by means of voltage-controlled automatic capacitors.

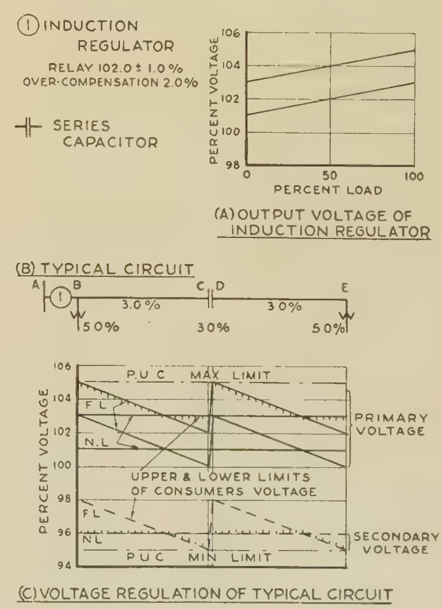

Figure 5. Voltage regulation of typical distribution feeder by induction regulators at substation and series capacitors on poles

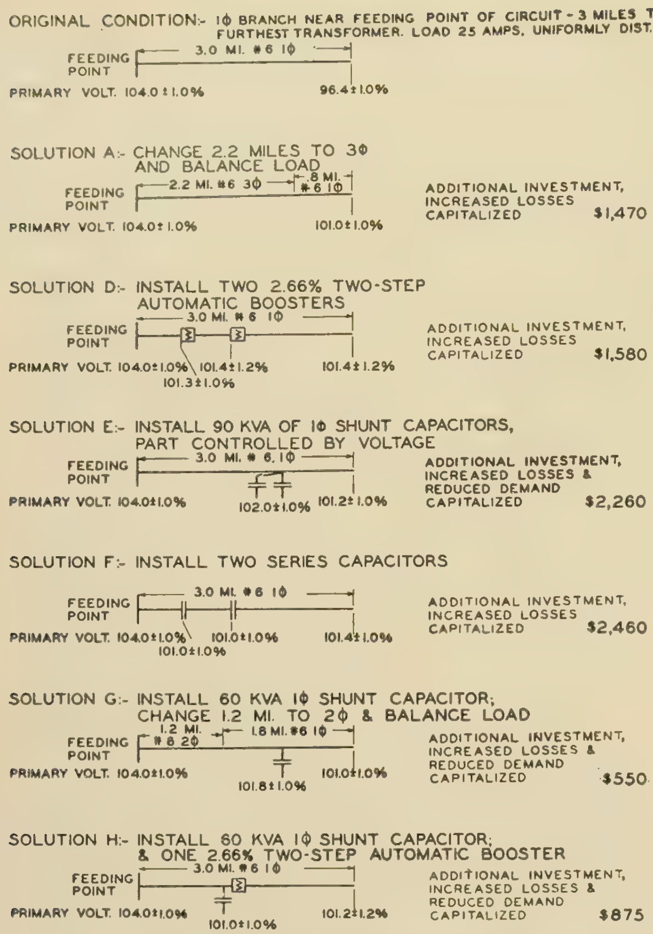

Figure 6. Economic comparison of several methods of improving voltage on a three-mile single-phase branch at the feeding point of a three-phase four-kv distribution feeder

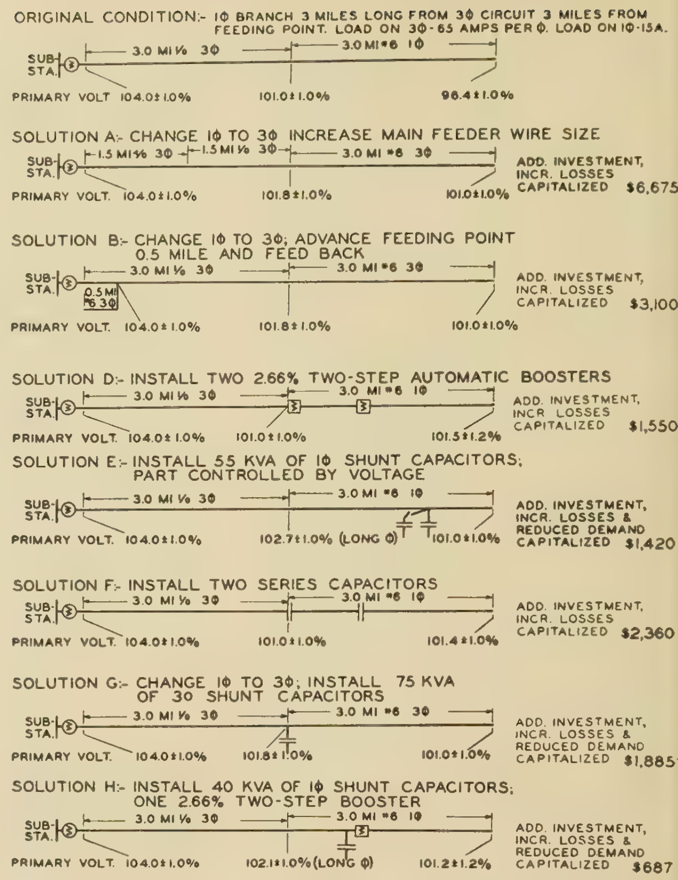

Figure 7. Economic comparison of several methods of improving voltage on a three-mile single-phase branch at the end of a three-phase four-kv distribution feeder

5. Reduce voltage drop in the existing circuit by means of series capacitors, which can be selected to give a voltage rise to compensate for the voltage drop in the conductors.

Several methods may be used in combination, such as

6. Install as much fixed shunt capacity as circuit conditions permit, advancing the feeding point or rebuilding the substation end of the circuit to secure the remainder of the required voltage correction.

7. Install as much fixed shunt capacity as circuit conditions permit, adding automatic voltage boosters to correct for excessive drop beyond the shunt capacitors.

Any of these methods can be applied to correct the voltage regulation of a distribution circuit which exceeds the permissible limits of steady-state voltage. Economics now enters as an important factor in selecting the one method most applicable to the case at hand. The previous paper presented a number of comparisons of the first three methods applied to typical circuits. These examples, corrected for increased construction costs and with the new methods added, are presented herein as figures 6, 7, and 8.

Shunt capacitors automatically connected during periods of low voltage and disconnected during periods of high voltage, used in combination with fixed shunt capacitors to secure more voltage correction than can be secured with fixed capacitors alone, did not in any case give as low a total cost of correction as could be attained with the combination of automatic boosters and fixed shunt capacitors. The automatic shunt capacitor does not seem to offer any real advantage and may well be dropped from further consideration as a device for general use to correct voltage on distribution feeders.

The series capacitor also is higher in total cost than several other methods and need not be considered further as a means for the correction of voltage drop in distribution feeders. An exception must be made, however, for fluctuating loads which require the reduction of circuit impedance either by increasing copper size or by the installation of series capacitors or proper current rating and capacitance, but this is a problem in itself, entirely separate from the scope of the present paper.

The lowest total cost in every case is secured by the combination of several methods. The installation of fixed shunt capacitors well out on the circuit near the point where the peak-load drop from the feeding point approaches 5.0 per cent almost pays for itself in the value of the system capacity released by the reduction in amount of reactive current to be supplied, and provides considerable voltage correction at low net cost. Fixed shunt capacitors may be added until the light-load voltage reaches the maximum or the circuit power factor reaches unity, whichever comes first. Any additional voltage correction required can be secured by means of automatic voltage boosters beyond the shunt capacitors.* The installation of shunt capacitors and one set of automatic boosters compensates for 5.66 per cent voltage drop and permits loading the circuit to 288 per cent of the limit established by voltage drop without any of these supplementary devices. Additional automatic boosters might be installed, but sooner or later the point is

* Fixed shunt capacitors should not be installed beyond voltage boosters as there are no compensators to overcome undesirably high voltage during light-load periods.

reached where it is more economical to rebuild the circuit for greater capacity and better regulation. This point depends largely upon the length of circuit and the conductor sizes, but it is doubtful whether two sets of boosters in series beyond a maximum installation of fixed shunt capacitors can be justified on four-kv circuits up to five or six miles long.

Long single-phase branches connected near the feeding point of a three-phase circuit also should have fixed shunt capacitors. Any additional voltage correction required should be secured by extending the second and third phases; considerable voltage improvement is accomplished by a comparatively short extension of the additional phases and the cost generally is less than that of automatic voltage boosters on the single-phase circuit.

Adaptability to Growing Loads

The final test of any proposal affording comparable service is whether its use results in lowered total costs over a period of years. The earlier paper showed the annual cost through a 30-year period of growth of two typical circuits using automatic voltage boosters for voltage correction as compared with the conventional methods of rebuilding. The same two circuits have been analyzed using fixed shunt capacitors to the limits of unity power factor and 3.0 per cent voltage rise, followed by other methods of correction.

In the first case, the circuit originally consisted of two number 3 copper wires single-phase extending three miles from a feeding point having peak load voltage of 104.0 ± 1.0 per cent. The load originally was 20 kva distributed uniformly along this three-mile feeder and the voltage drop was entirely satisfactory, but load growth at the rate of ten per cent per year soon increased the drop to the limit. At this point a shunt capacitor is installed two miles from the feeding point to correct the voltage. Continued growth exhausts this means of correction and the circuit is then gradually converted to three phase. The steps are shown in figure 9 along with correction by boosters and by circuit rebuilding alone. The relative costs are plotted in figure 10. It is obvious that the conversion from single-phase to three-phase is delayed, with a savings of four per cent in accumulated annual cost.

In the second case the circuit originally consisted of four number 3 copper wires three-phase extending three miles from a feeding point having three-phase voltage of 104.0 ± 1.0 per cent. The load originally was 100.5 kva uniformly distributed along the feeder and well balanced, and the voltage drop was satisfactory, but load growth at the rate of ten per cent per year soon increased the drop to the limit. At this point fixed shunt capacitors are installed two miles from the feeding point to correct the voltage. Continued growth necessitates further correction and a set of automatic voltage boosters is added, followed by circuit rebuilding and additional capacitors. The steps are shown in figure 11, along with correction by boosters and by circuit rebuilding alone, and the relative costs are plotted in figure 12. Again it is clear that the capacitors and boosters together have delayed the more expensive rebuilding and have accomplished a substantial savings in annual costs.

Conclusions

These growing-load comparisons confirm the conclusion indicated by the sev-

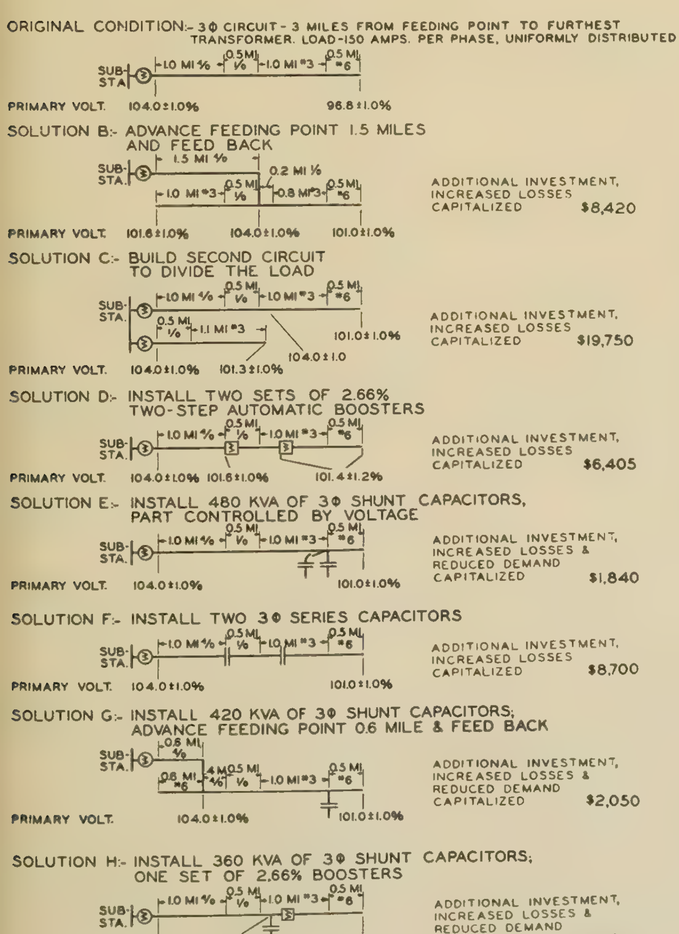

Figure 8. Economic comparison of several methods of improving voltage on a three-mile three-phase four-kv distribution feeder

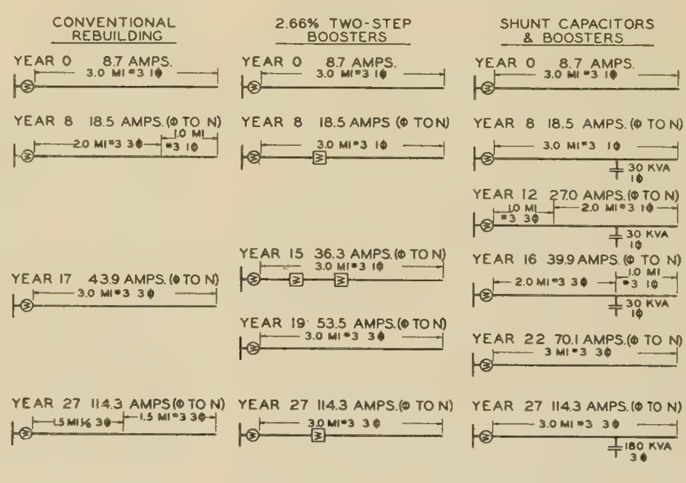

Figure 9. Periodic alterations on three-mile 2,300-volt single-phase branch at the feeding point of a three-phase four-kv distribution feeder to maintain primary voltage within limits of 105 and 100 per cent for load growth of ten per cent per year

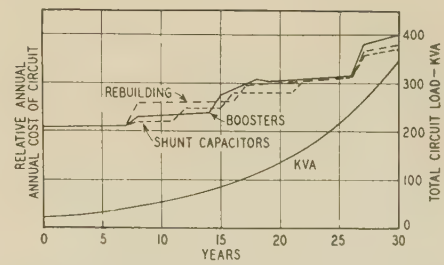

Figure 10. Relative total annual cost of serving a load growing ten per cent per year on a three-mile 2,300-volt feeder initially single-phase with number 3 conductors

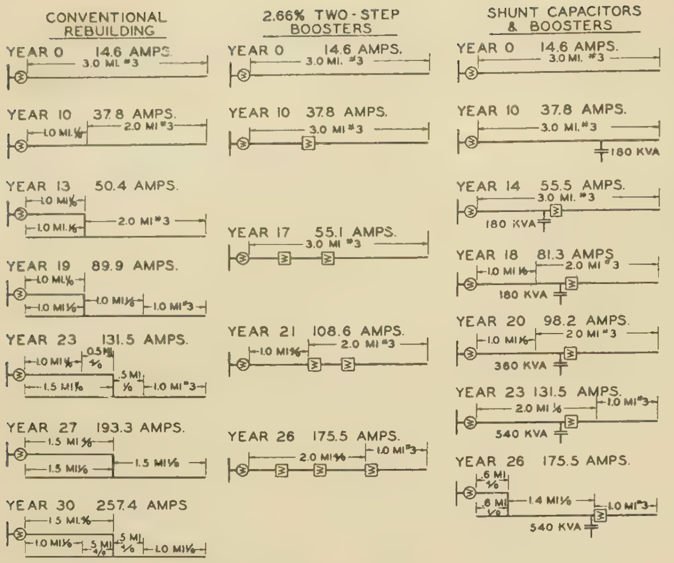

Figure 11. Periodic alterations on three-mile four-kv three-phase circuit to maintain primary voltage within limits of 105 and 100 per cent for load growth of ten per cent per year

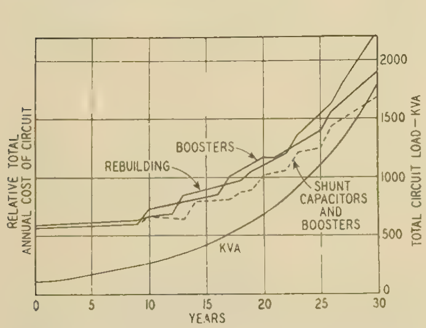

Figure 12. Relative total annual cost of serving a load growing ten per cent per year on a three-mile feeder initially four-kv three-phase with four number 3 conductors

eral static comparisons that correction of voltage on four-kv distribution circuits by means of fixed shunt capacitors to the limit established by power factor and by light-load voltage, followed, when additional correction becomes necessary, by one set of automatic voltage boosters beyond the capacitors or by extension of phases in the case of a single-phase tap from near the feeding point, makes possible substantial savings in the cost of maintaining proper voltage at all points on the circuit. It was very obvious at many points in the comparisons that careful adherence to this policy is essential to maximum economies and that it is unwise to attempt to use too many auxiliary regulating devices before installing 1/0 or 4/0 conductors in the heavier-loaded portions of the circuit.

Only three types of supplementary voltage-regulating devices are needed for suburban four-kv feeders. Single-phase shunt capacitors of 30 kva are used for the initial voltage correction on single-phase extensions, followed by single-phase five-kva automatic voltage boosters with two 1.33-per-cent steps except on a single-phase tap from near the feeding point which should have additional phases extended. Low voltage on the three-phase portions of circuits is corrected by means of three-phase four-wire shunt capacitors of 90 to 180 kva, followed by sets of three of the same five-kva automatic boosters. The storeroom setup is thereby simplified, and there is better opportunity for prompt reinstallation of the supplementary regulating devices after they are removed from their initial locations. This system provides worthwhile economies in the cost of distribution, and delays major changes until more load has developed to give a better indication of future needs.

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Discussion

P. E. Benner (General Electric Company, Schenectady, N. Y.): Mr. Olmsted has certainly done a very commendable job in presenting this straightforward and clear-cut method of analyzing the operation and evaluating the benefits of using shunt capacitors in connection with automatic boosters. He has shown that by proper application of capacitors installed for power factor correction it is possible to double the primary drop in the load area without increasing the limits of consumer voltage variation. This makes possible the use of smaller conductors or an increase of the load area served by any particular feeder.

The author arrives at a value of three per cent as the maximum permissible correction by means of shunt capacitors. In this connection it is important to note that this three per cent correction is between the first transformer and the capacitor installation, and does not refer to the voltage rise from the capacitor clear back to the substation. It should be further noted that this three per cent correction or voltage rise does not take into account the possibility of using a branch feeder regulator with a buck as well as a boost range installed between the capacitor installation and the first transformer. The installation of such a branch feeder regulator with line-drop compensator would, of course, double the permissible voltage rise due to the capacitor, thereby raising the limit of three per cent to six per cent.

By limiting himself to the use of a two-step booster the author cannot take full advantage of shunt capacitor applications in that in many cases it would not be possible to locate the capacitor as far out on the circuit as might otherwise be desirable. It is believed that the small cost differential between the two-step booster and the four-step regulator with line-drop compensation would easily be offset by the postponement of the time of changing a circuit to three-phase or installing larger copper. The four-step regulator of conventional design can be connected for two steps raise and two steps lower, and therefore would permit the use of a shunt capacitor located further out on the line. Such an installation would make it possible to double the allowable primary drop as shown by the author in figure 3.

In addition to the author's five methods for correcting the voltage regulation of a circuit, it would seem desirable in a comprehensive analysis to consider the use of a branch feeder regulator of either the four-step or the induction type which have a buck as well as a boost range and are therefore particularly adaptable for use in conjunction with a shunt capacitor.

Also, in cases where voltage regulation is the primary consideration one should not overlook the possibility of obtaining the desired voltage rise by means of a fixed booster installed in the circuit in the load area as discussed in the paper, "Voltage-Regulating-Equipment Characteristics as a Guide to Application" which was published in the 1938 AIEE Transactions (September section). It is usually possible to obtain voltage correction by means of a fixed booster for only about 10 to 15 per cent of the cost of obtaining it by means of a shunt capacitor.

Raymond Bailey (Philadelphia Electric Company, Philadelphia, Pa.): The discussion in Mr. Olmsted's paper relating to credit for capacity released in a system by the use of static capacitors leads to a more general question as to conditions under which full credit for such system capacity should be allowed. From the point of view of sound business, it would seem reasonable only to allow full credit on the basis of cost per unit of capacity installed in those cases where this released capacity permits taking on load where otherwise investment would have to be made in the immediate future to provide the capacity for this load. For ex-

ample, it is questionable how much credit should be allowed in a case where the use of static capacitors on distribution circuits releases capacity in the step-down transformers in a substation which are at present rather lightly loaded, as it may be an appreciable number of years until this released capacity is actually needed to carry load. A more or less parallel case exists in determining credit for released generating capacity and here one must consider what actual effect this released capacity would have on the future investment to provide generating capacity.

It will be found in many cases that it is sound business to allow as a credit only a certain proportion of the cost per unit of capacity in place, depending upon the time that will elapse before this capacity is actually needed.

This matter of value of system capacity is one that frequently arises in economic problems that have to do with public utility systems and it is believed that it deserves a very searching review by public utility engineers in order to avoid balancing on one hand an actual expenditure of money against calculated credit which may not represent a real saving.

C. E. Arvidson (The Commonwealth and Southern Corporation, Jackson, Mich.): Mr. Olmsted's paper shows economic comparisons of various methods of correcting excessive voltage drops in distribution circuits and justifies the use of capacitors largely by capitalizing the released system capacity resulting from the capacitor installations. Does this released system capacity have any value during an interim when the transmission lines and substations involved are not loaded to capacity? In cases where the transmission lines or substations are loaded to capacity, does not the installation of capacitors serve to postpone the installation of additional system capacity rather than replace such additions? If capacitors can be considered as a means of permanently releasing system capacity, should not investigations be made to justify their installation on all distribution circuits regardless of existing voltage conditions?

It is the writer's opinion that the economic value of capacitors as a means of voltage correction depends upon existing conditions and that other methods of voltage correction may often be more economical than capacitors.

L. H. Hill (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): Mr. Olmsted is to be congratulated on his very thorough discussion of various means of regulating distribution feeders. It is evident from the paper that the elimination of wattless current on distribution feeders is highly desirable. In this regard the author apparently has overlooked the great saving in exciting kilovolt-amperes obtained by the use of step-type feeder voltage regulators instead of the induction type as referred to in the paper.

For example, induction-type regulators used to regulate the 1,500-kva feeder referred to in the paper require approximately 37.5 kva for excitation. On the other hand, step-type regulators for this same service require only about 7.5 kva for excitation—a net saving of 30 kva. This means that 30 kva, or almost ten per cent of the capacitors, could be eliminated by the substitution of step-type regulators for the induction type now used. Since capacitors are priced at about $8.00 per kilovolt-ampere, this would result in a direct saving of $240.00.

If capacitors were not used, an even greater advantage can be shown for the step-type regulator. The reduction of 30 kva in wattless current on this particular feeder would result in a decrease in demand of 12.4 kva. When capitalized at $50.00 per kilovolt-ampere according to the paper, this reduction in demand would amount to a saving of $620.00.

From these two illustrations it is evident that much can be done in the way of selecting equipment to release system capacity without resorting to capacitors. Even if capacitors are found desirable, however, the step-type regulator greatly reduces the number required.

J. W. Butler (General Electric Company, Schenectady, N. Y.): In view of my interest in the application of series capacitors I would like to comment briefly on their use in the circuit regulation picture, in addition to that considered by Mr. Olmsted.

Admittedly this paper and its presentation was not required to dwell on this point since the objective was to determine the most economical way to take care of the type of regulation that could be handled in several different manners as listed in figure 8. Series capacitors, however, are able to furnish a type of regulation in addition to that type dealt with in the paper that can be furnished by no other piece of regulating equipment. This discussion is given for the benefit of those individuals not intimately acquainted with the subject in case their opinion of the series capacitor was formed in substance by the account given by Mr. Olmsted.

A series capacitor gives a boost in the line voltage at the point of application as determined by its ohmic value and the power factor of the current. This boost takes place at the capacitor—hence the circuit on the power-source side has the same regulation as it had before installation. The voltage on the load side, however, is changed and for any given power factor and circuit, the voltage at a preferred location can be made to have practically zero regulation. The rest of the circuit, however, will have regulation, either plus or minus, depending upon its location with respect to the capacitors. Obviously then for a circuit having a distributed load, two or more series capacitors probably would have to be used to give satisfactory regulation to the complete circuit. It is this point that makes the series capacitor appear in an unfavorable light when considering Mr. Olmsted's problem.

Consider however a feeder having substantially no load taken from it until it gets to a bus, where there might be a saw mill, an electric dredge, a large motor that is intermittently started, or some other type of pulsating load that causes severe voltage fluctuations at the bus affecting all other connected loads, such as city lighting. Since the series capacitor produces its compensation *automatically* and *instantaneously*, it enjoys a unique position in the regulating field in being able to cure this type of voltage fluctuation.

Paul H. Jeynes (Public Service Electric and Gas Company, Newark, N. J.): This paper deals with a subject that has been carefully investigated by Public Service engineers, and it can be said that in general our experience and conclusions parallel closely those reported by Mr. Olmsted. Differences in detail may perhaps be accounted for by peculiarities of load conditions on the two systems.

For example, the statutory voltage tolerance in New Jersey is most rigorous, being plus or minus three per cent—not five per cent. In addition, we are acutely conscious of the error in supplying unnecessarily high voltage to the customer, both on ethical grounds and for the sake of good public relations. Although studies have demonstrated that the effect of increased voltage (within this three per cent) on customers' bills is practically negligible, this factor is vastly overrated in the minds of many—engineers as well as laymen. The use of the shorter-lived Mazda lamps is liable to create a suspicion that unduly high voltage may be responsible for the increased lamp renewals. In either event, it is important to be able to demonstrate that infinite pains are taken to avoid the condition.

Other characteristics of the Public Service system are the high power factors ordinarily encountered at time of peak loads, and the moderately high load density in the territory served. While we do have areas that we consider to be "rural," by some standards even these would be classified as zones of fairly good density.

One apparent difference which results from the above considerations is the matter of compensating primary circuits. Maximum permissible voltage is not maintained at the point of feed—that is, the first transformer on the circuit—unless that is necessary in order to keep the last customer from dropping below the permissible minimum. In other words, circuits are compensated to maintain nominal voltage at some point well out on the primary, which tends to reduce the voltage improvement to be expected from shunt capacitors attached at the end of the circuit.

Another effect of the small voltage tolerance is that the range of single-step boosters must be limited to five per cent, as a rule, while no applications for multistep boosters have yet been found.

Automatic Boosters

A number of automatic-booster installations are giving satisfactory service and have proved to be cheaper than any alternative. On a few occasions boosters were installed only to have it appear that this was the wrong answer. The difficulty lay in the type of load, which resulted in an almost unpredictable daily voltage cycle, and too frequent operation of the controls. In one case we were able to avoid trouble by using a time switch to cut out the automatic operation during daylight hours when the circuit supplied only power load, which caused the difficulty.

Like Mr. Olmsted we had discovered that the combination of booster and shunt capacitor is ideal for certain situations, but refrained from reporting on the subject only because we have made no actual installations. Our nearest approach is a recently completed combination of pole-type induction regulator with capacitors at the end of

single-phase branches beyond the regulator. This has solved a difficult problem and is giving almost ideal service.

Shunt Capacitors

As Mr. Olmsted points out, light-load conditions usually limit the size of capacitor installations for voltage improvement. About 1,000 kva of 15-kva units are installed on Public Service lines, and we are enthusiastic about them. They give maximum relief at the point of attachment, where it is most needed, and the effect tapers off back to the center of regulation; as a result, they minimize the undesirable overvoltage supplied to customers nearer the substation, which is just the reverse of a boost applied part way out on the circuit.

We have not as yet made any installations switch-controlled by the voltage, though we have numerous units on pole-type constant-current transformers that cut in with the transformer when the street lights are turned on. The proposal for voltage control of capacitors connected does have appeal, particularly because the voltmeter would be located at the point of maximum voltage variation. This of course is not the case for boosters; the resultant debate over voltage versus current control of boosters is familiar to all. In any event, it appears to be purely a matter of relative costs; we are not yet ready to rule out automatically controlled capacitors as a future possibility.

"Released Capacity" Savings

On this subject of savings from "released capacity" in the supply system we are forced to differ with Mr. Olmsted—not in criticism of his figures, but of his general approach to the problem of evaluating savings. An important addition to his bibliography is proposed, being a brief comment by Alex Dow which appeared in the *Electrical World* for January 10, 1925.

Sometimes fantastic savings result from power-factor improvement. But unless *without* capacitors we would have to spend money that would not be spent *with* capacitors, these "released capacity" savings are worth zero dollars per kilovolt-ampere. Installations of the small amounts of leading reactive capacity required for voltage improvement very rarely affect the program for feeder, substation, or transmission capacity installations, in our experience. Even when capacitors can be justified by resultant postponement of capacity installations, the postponement is only temporary; a point is reached where the power factor approaches unity, the capacity must be installed in any event, and the capacitors are for a time of no further use.

The most fruitful source of savings in our experience lies in postponement of incremental substation transformers, and this possibility of savings is investigated whenever growing loads indicate that additions are imminent. Frequently postponement for a short period is found justified—usually two to four years—provided immediate use can be found for the released capacitors when, at the end of that time, the transformers must be installed in any event. Usually, also, no coincidental installations of transmission or other capacity are on the books during that period, so that the hypothetical "released capacity" savings beyond the transformers are nil.

The fantastic savings mentioned above were realized when, after a small installation of capacitors had made the postponement effective for one year, the load unexpectedly dropped off. Had the transformers been installed we would now be saddled with carrying charges of perhaps $5,000 annually, but the capacitors, having effected that saving, have been transferred to other locations where they are earning their keep and compounding the savings.

To sum up the foregoing argument, it seems wise to ignore possible prorata kilovolt-ampere savings from "released capacity" unless their specific source can be pointed out. If capacitor installations are justified despite this omission, any real credit from such a source is "velvet."

We have been unable to formulate any general rule to indicate the economic preference for polyphase, increased wire size, boosters, or capacitors; each case requires individual study.

Series Capacitors

Public Service experience with series capacitors is adequately covered by Mr. Olmsted's bibliography. At the present time another similar installation is being considered, and like its predecessors it is proving to be a delicate engineering problem. Series capacitors resemble the well-known little girl with a little curl right in the middle of her forehead—when they are good they are very, very good.

L. M. Olmsted: Mr. Butler calls attention to the fact that the series capacitor, in addition to its ability to correct steady-state voltage conditions, has the unique advantage of being instantaneous in effect and thereby correcting fluctuating voltage conditions too rapid to be corrected by any of the other devices. In a paper specifically limited to the correction of steady-state conditions it seemed unwise to discuss fluctuating voltage. Consequently the series capacitor, found uneconomical for steady-state conditions, was dismissed with only a suggestion as to its other advantages. I am glad that Mr. Butler has discussed the most advantageous field for the series capacitor and corrected any impression that the series capacitor is never desirable.

Messrs. Arvidson, Bailey, and Jeynes all have commented on the credit for system capacity released by the power-factor improvement coincident with the correction of low voltage by means of shunt capacitors. Admittedly the value of this system capacity depends upon the ability to postpone the installation of additional system generating, transmission, substation, or distribution facilities. Every case is different, and the true value ranges from almost zero to such "fantastic savings" as mentioned by Mr. Jeynes. The same question arises in the evaluation of losses, in attempting to assign a fair increment above the fuel cost to cover the use of system investment. The figure of $50 per kilovolt-ampere of system capacity released by the capacitors is consistent with the capitalized valuation of losses (omitting generating capacity which is already operating above rated power factor). Raising or lowering this credit for released capacity to suit some particular conditions might affect the choice of shunt capacitors for economical correction of voltage, might even justify the installation of shunt capacitors where voltage is satisfactory solely to postpone system expansion, but it cannot affect the economic advantage which all of the supplementary regulating devices hold in common over conventional line rebuilding, the advantage of high reclaim value for use elsewhere.

Mr. Jeynes' comments indicate very close similarity indeed between Public Service findings and our own. Even though the statutory limits in Pennsylvania are more liberal, the many thousands of voltage checks made by our troublemen on their service calls indicate that 90 per cent of our consumers receive voltage within ± 2.5 per cent of nominal. We consider it advisable to restrict the amount of boost at any one location to less than 5 per cent but favor two small steps instead of one larger step. The apparent difference in the method of compensating primary circuits for line drop probably is negligible, as we have only recently changed from the method described by Mr. Jeynes to the one described in this paper in order to secure more accurate supervision over the voltage actually supplied to all consumers at their service switches; it is designed to check the upper and lower limits of voltage instead of testing the average consumer.

All of our automatic boosters are equipped with time delay of approximately 50 seconds. This delay has been found desirable to permit close voltage settings without too frequent operations of the tap changers. No conditions have been encountered where this arrangement has permitted excessive operations.

Mr. Benner points out that line-drop compensation can be added to pole-mounted voltage regulators, after which shunt capacitors might be installed at the ends of the circuit. With this combination, the voltage boost of the shunt capacitors benefits the entire circuit during heavy-load periods and overvoltage during light load is prevented by bucking action in the pole-mounted regulators. This combination was not considered in the paper because it was thought desirable to keep pole-mounted equipment as simple as possible and also because of the difference in cost between pole-mounted regulators and two-step boosters, but its feasibility is proved by Mr. Jeynes' discussion and it may have greater economic advantages than I had anticipated.

The fixed booster undoubtedly gives inexpensive voltage correction and should be considered, especially if no value is placed upon the released capacity coincident with voltage correction by means of shunt capacitors. In one case fixed boosters have been considered to correct for drop in a long feeder, but the cost was high because of the current to be carried and shunt capacitors near the ends of the branches, credited with reduction in losses and released capacity, gave much lower net cost.

Mr. Hill points out a very interesting advantage which step-type regulators have over the induction type. The lower exciting current, in combination with lower price, certainly merits some thought.

All of the discussers have been most helpful in adding to the information available and in indicating various other problems to consider in the effort to secure necessary voltage correction in the most economical way.

Transactions

Papers and Discussions Comprising Pages 499–562 of the 1939 Volume

The Rating of Electrical Machinery and Apparatus

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THIS PAPER outlines briefly some of the technical, economic, and psychological aspects of the methods of rating electrical machinery and apparatus. Some proposed modifications of existing standards also are discussed, with the idea of bringing the standards into better agreement with present-day conditions, knowledge, and practices.

I. Objectives of an Ideal Rating Structure

Before discussing the present structure of rating standards and any changes therein, it may be well to set forth briefly the objectives of an ideal method of rating.

1. The ratings assigned to individual machines or devices under any adopted structure of standards should convey correct and useful information to their users. Therefore, they should indicate the load which can be carried with safety either continuously or for specified periods, and they furthermore should give reasonable assurance of satisfactory and economical operation from every point of view under typical or normal conditions. This might well be considered the primary purpose in assigning ratings. The information conveyed by the method of rating should, of course, make possible a fair comparison of competitive commercial products.

2. The system of standards should tend to bring about the most economical all-round application of electrical machinery and devices.

3. The entire structure of standard ratings should be as simple as possible in order to avoid confusion and consequent misapplication. Thus, the various American standards should be as uniform as possible and they should be as closely in accord with the international standards as they can be without interfering with requirements resulting from the particular conditions prevailing in this country.

In any attempt to meet these objectives, the psychological aspects of the rating structure should be given as much attention as the technical and economic. It should be realized that if the stamp of approval is placed upon certain values of temperature rise and corresponding ratings by experts in the industry, it should, and does, carry considerable weight with the laymen, the less informed technicians, and the engineers and creates the impression that such temperature rises and ratings are approximately correct for normal operation in actual service. The fact that a simple method of rating cannot give complete information for all conditions of service is no reason why the rating given to a machine should not be indicative of typical or normal conditions. If, for instance, ratings for class *B* railway motors are based on a temperature rise of 105 degrees centigrade, as has been done in one of the International Electrotechnical Commission standards, instead of on a rise of 120 to 130 degrees centigrade, which has been demonstrated to be both satisfactory and economical in the United States, the operating engineer naturally will not load the motors in service so that they operate at the higher temperature rises. In other words, the IEC method of rating undoubtedly will result in uneconomical applications. This illustration brings out a point of view entirely different from the one frequently expressed, namely, that the method of rating is useful merely in facilitating a fair comparison between competitive products.

As in many similar cases, it is impossible to meet these objectives in full because recognition cannot be given to some conditions without interfering with others. For example, a rating structure taking into account the most economical application may not at the same time have the desired simplicity; in which case, a reasonable compromise between the conflicting objectives should be adopted.

II. Present Standards and Practices

Almost since the beginning of the electrical industry, the basic principle of rating electrical apparatus has been to specify the temperature rise permissible either for continuous operation or for limited periods. The basic considerations, limiting values of temperature, and methods of testing are covered in AIEE Standard No. 1,[1] while standards applying to specific types of machinery are covered in various National Electrical Manufacturers Association and American Standards Association rules. Standards for international use have been established by the IEC. Since the maximum temperatures reached by the insulation cannot be determined by any of the present methods of determining temperature, namely, the thermometer, resistance, and embedded-detector methods, values somewhat below the maximum permissible temperatures have been selected as standards. The difference between the maximum temperatures and those obtained with the available methods of test varies with different designs; however, values covering conditions found in conventional designs have been selected as standards for these differences.

Although AIEE Standard No. 1 gives all three methods of measurement, the ASA and NEMA standards recognize only one of the three methods for some types of machines and their individual parts, the thermometer method being given in the majority of cases.[2] In other instances, as, for example, the tentative standards for traction apparatus, both the thermometer and the resistance method are recognized, but "the resistance method is considered as the rule."[3] Both the ASA and IEC transformer standards[4] specify the resistance method for windings. The international standards for machinery[5] specify embedded detectors for large

Paper number 39-6, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 31, 1938; made available for preprinting November 29, 1938.

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1. For all numbered references, see list at end of paper.

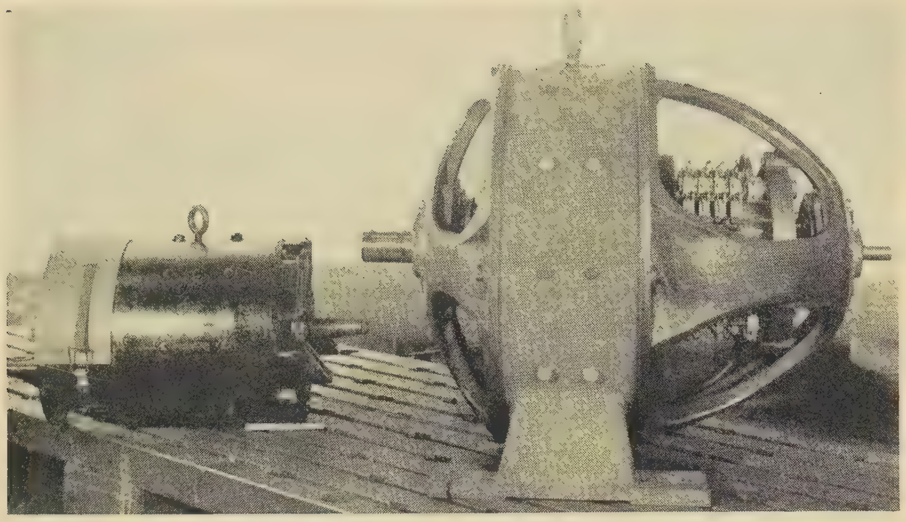

Figure 1. Comparison of a modern traction motor and a general-purpose industrial motor

Left—Traction motor, 125 horsepower based on temperature rise by resistance, 120 degrees centigrade in armature, 130 degrees centigrade in field; continuous speed, 1,450 rpm; weight per horsepower, 8 pounds

Right—Industrial motor, 125 horsepower based on temperature rise by thermometer, 40 degrees centigrade; continuous speed, 1,150 rpm; weight per horsepower, 25 pounds

machines, but in most other instances they recognize either the thermometer or the resistance method for windings, with implied preference for the resistance method. The thermometer method is of course specified for such parts as cores, commutators, etc., where the resistance method is not applicable. The same general practice is followed in the international standards for traction machinery,[6] but recent trends indicate that for this application the resistance method will practically eliminate the thermometer for any windings. (The subject of temperature measurement is discussed further in two contemporary papers.[7,8]) A greater degree of uniformity between these standards obviously is desirable, because with present-day knowledge there seem to be no sound reasons for some of the differences existing.

The standard temperature rises for continuous ratings usually have been selected with the intent of permitting continuous operation without injury to the insulating materials under an assumed ambient temperature of 40 degrees centigrade. An exception to this is the new ASA transformer recommendations which specify operation at an average ambient temperature of 30 degrees centigrade and a maximum of 40 degrees centigrade. The tentative standards for traction motors assumed, by inference, an average ambient of 25 degrees centigrade and a maximum of 40 degrees centigrade.

In addition to the continuous ratings discussed so far, overload ratings with temperature rises higher than those specified for continuous load are provided by some of our national standard rules for some types of electrical machines and apparatus. This is considered justified by the fact that deterioration of insulation is a question of both temperature and time and that satisfactory life can be obtained if higher temperatures are maintained only for short periods and at not too frequent intervals. Other commercial standards specify short-time ratings, the most typical among which is the one-hour rating for traction motors, which like any other short-time rating indicates to a certain extent the heat-absorption ability of the machine. (Short-time ratings are discussed in a contemporary paper by L. E. Hildebrand.[9])

In some instances the basic temperature rises specified in AIEE Standard No. 1 have been departed from, usually, however, for sound technical or economic reasons. For example: The temperature rise, by thermometer, for general-purpose motors has been specified as 40 degrees instead of 50 degrees centigrade, the limit given in Standard No. 1. This was done for the reason that general-purpose motors are often applied where the load is not exactly known and also by non-technical users, and a somewhat greater margin of safety was therefore considered advisable when the 40-degree standard was adopted. An extreme in the other direction is the temperature rise of 120 degrees, by resistance, specified for the armature and 130 degrees for the field in the tentative ASA standards for traction motors in comparison with the 75-degree rise specified in AIEE Standard No. 1. For traction work, higher temperatures and possibly a shorter life of the insulation may be justified by the economic gains possible through a reduction in the weight of and space occupied by the motors. Those familiar with railway problems know how important a reduction in size and weight is, not only in the motor itself but also in the resultant reduction in the truck and other parts. The use of small motors with higher temperatures in locomotives often makes it possible to reduce the number of driving axles and thus materially reduce the length and weight of the entire locomotive. In class *A* railway motors, armature coils have been found to have a shorter life than in industrial applications, but the expense for rewinding more frequently is compensated for by other economies gained. There is considerable evidence that class *B* insulation used in railway motors lasts almost indefinitely in spite of the increased temperature rises over those given in other standards. It is thus evident that in railway work a departure from the usual limits of temperature rise is fully justified by both economic considerations and actual practice.

Figure 1 shows the contrast in size which has resulted from the two most extreme departures from the temperature rises specified in Standard No. 1. At the left is a modern traction motor with class *B* insulation and designed for temperature rises (by resistance) of 120 and 130 degrees for the armature and field, respectively. At the right is a general-purpose motor of the same horsepower rating designed for 40 degrees temperature rise (by thermometer). The difference in the size of the two motors is marked; the weight of the traction motor is only about one-third that of the industrial motor. The speeds of the motors are different, but in both cases are in accord with those commonly used in the most modern practice for the particular applications. The fact that the large machine is not appreciably higher in cost than the small traction motor is surprising, but this is merely further evidence that the practices established for the two extreme cases are justified for the particular conditions to be met. While the adoption of the railway practice for general-purpose motors would result in reduced dimensions and weight, this would be of little value in most applications. On the other hand it would mean disadvantages such as less accessibility of parts, reduced overload capacity on account of reduction

in mass, etc. The condition illustrated by the particular case cited—namely, that higher temperature rises are instrumental in reducing weight and size but have only a minor effect on cost—is found to hold true in many cases, particularly with the smaller sizes. The reason for this is that under crowded conditions of assembly, the expense for labor is likely to increase, and also because a change from class *A* to *B* insulation means an additional increase in cost of material with the present market prices of the class *B* materials involved.

The temperature rise of 55 degrees specified in the ASA standards for enclosed ventilated motors is another departure from the value specified in AIEE Standard No. 1. The difference of 5 degrees was adopted on the ground that temperatures within enclosed motors are usually more uniform than in open motors, resulting in a smaller difference between the hot-spot and measurable temperature. Furthermore, there is likely to be a decreased tendency toward deterioration of the insulation in fully protected windings.

It was early realized that the maintenance of safe temperatures was not the only condition to be met by commercial machines and apparatus. In addition to this, generators and transformers must have proper regulation; motors of all kinds must have sufficient starting and pull-out torque to meet prevailing service requirements; all commutating machines must give satisfactory commutation; machines and apparatus of all kinds must have efficiencies resulting in economical operation; a-c machinery must meet certain power-factor conditions; starting currents of motors must be limited to tolerable values; etc. In the realization of this, various standards set up by NEMA and ASA specify standard limiting values for the factors just mentioned. In arriving at these standard values, allowance has been made for reasonable variations in the power supply, particularly with reference to voltage. In practically all cases, however, the temperature rise remains the primary basis for rating and the other factors are usually looked on as supplementary standards.

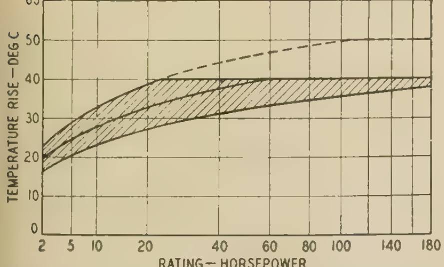

Figure 2. Band indicating actual temperature rises of general-purpose induction motors—four, six, and eight poles

In actual practice it frequently happens that in order to meet the requirements just mentioned, the apparatus has to be larger than dictated by temperature considerations, which in turn means that the actual temperature rises will be considerably below the specified standards, particularly for the lower ratings. In order to have economical manufacturing and stock conditions, the number of frame sizes for any line of machines or apparatus is limited, and as a result a frame size somewhat larger than necessary for the particular rating often has to be used, which again results in the actual temperature rise being lower than the specified standard. Figures 2, 3, and 4 show actual conditions for several types of apparatus.

Mention should be made of an increasing tendency to equip apparatus with thermal protective devices. Recent improvements in these devices and especially the high degree of protection which is possible with transformers are bound to result in better and broader utilization of the protected apparatus. At present the advantages of these protective devices are utilized in the application of apparatus, but eventually their use may influence the methods of rating.

III. Discussion of Changes in Rating Structure

One of the most important advantages of standards lies in the fact that valuable application experience is accumulated on the standards selected and any change nullifying such experience is likely to result in marked economic losses. Therefore, changes should be avoided unless worth-while advantages can be secured through them. In considering possible improvements in existing standards, it seems advisable first to consider minor modifications which would simplify and unify the present national and international structures of standards without causing difficulties of the nature just mentioned. Secondly, the various general practices which have been established and which are not in accord with the standards or are not recognized therein should be studied. If a critical examination of these practices indicates that they are sound in principle, the standards should be revised to recognize them. Finally, it should be determined whether on account of new developments and experiences, certain changes in well-established standards and practices resulting therefrom are justified by the economies accomplished in the industry as a whole.

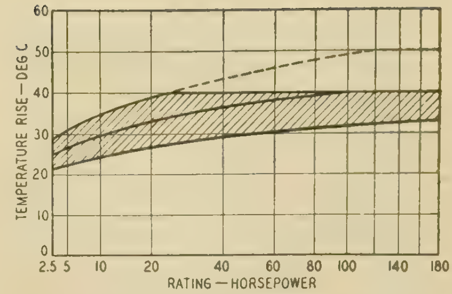

Figure 3. Band indicating actual temperature rises of general-purpose d-c motors having speed range of 850 to 1,750 rpm

Following are a number of specific suggestions which, with the exception of a few minor changes proposed, relate essentially to supplementary provisions in the standards and are more of an evolutionary character; consequently they will not appreciably affect any standards or practices already in existence.

1. Early attention should be given to the following modifications in AIEE Standard No. 1:

A. The value specified for the temperature rise (by resistance) of class *B* insulation should be changed from 75 degrees centigrade to 80 degrees centigrade, the value specified in the IEC rules. The experience previously cited with class *B* insulation on railway motors indicates that this change is perfectly safe; furthermore, it means no hardship to manufacturers since existing designs would meet the new standard. A change of the 55-degree value, by resistance, for class *A* insulation to the IEC value of 60 degrees also seems desirable if investigation shows it to be safe.

Better agreement between AIEE Standard No. 1 and IEC standards could further be reached by lowering the class *B* thermometer value of 70 degrees in AIEE Standard No. 1 to 65 degrees, the value specified by IEC. However, it seems that this would be a step in the wrong direction in view of our railway experience, and, furthermore, the new value could not be met by existing designs.

B. A "Class *B*-1 Winding Practice," as distinguished from the ordinary class *B*, should be established.

The fact that long life and undoubted economies have been obtained in traction work with temperature rises for class *B* insulation far in excess of those specified in Standard No. 1 naturally suggests the idea that this be recognized in any revision of this standard. In formulating the new standard, a number of stipulations could be made in addition to those for ordinary insulation. Among them, the use of high-temperature soldering or brazing materials for joints and bands could be specified, as has been found expedient in railway practice. Precautionary statements could be added,

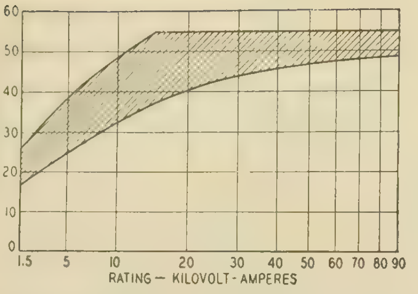

Figure 4. Band indicating actual temperature rises of distribution transformers of 2,400, 4,800, 6,900, 11,500, and 13,200 voltage classes

indicating, for instance, that the higher temperature rises should be adopted only where previous experience with machines of similar size and construction has demonstrated that satisfactory operation and life can be expected. (Various considerations which enter into this question other than direct deterioration of insulation by temperature are covered in a companion paper by C. Lynn.[10]) The availability of such high temperature standards would tend to establish a uniform practice if and when advantage is taken of the traction experience in other fields, such as aviation, navigation, and special industrial applications. Some consideration might be given to similar action in the use of class *A* insulation if this appears to be justified by a further study of operating experiences.

(As a matter of course, proper provisions for the application of glass insulation in connection with class *B* insulation and the suggested "Class *B*-1 Winding Practice" should be made in AIEE Standard No. 1 at the same time.)

C. Standard No. 1 should be revised to formulate and to recognize more definitely methods of rating and application other than those based on indefinite life with continuous full-load operation at an ambient temperature of 40 degrees centigrade.

Standards for reduced life expectancy should be established for use where this is considered economical. Recognition should be given to the usual condition of low average ambient temperatures, as has been done in the new transformer and traction apparatus rules. Short-time and intermittent-load conditions also should be covered by suitable provisions. These steps seem advisable because engineering and economic considerations cannot, and should not, be separated. A basic standard such as No. 1 should recognize their interrelation and establish guiding principles sufficiently flexible to cover varying economic conditions. Here again the available railway experience and practice may well serve as a guide. However, consideration should also be given to other economic conditions, as, for instance, motors built into various types of machinery, where on account of obsolescence or other factors, short life of the machinery is to be expected. In electrical machines for automobiles, for example, it would be a decided waste to apply insulation for an indefinite life expectance. Valuable data are given on intermittent-load applications in a contemporary paper by Alger and Johnson.[11]

Some of the points suggested may be covered by guiding principles for the application, but in other cases the rating structure may be involved and the most suitable procedure will have to be determined by further study.

2. In addition to the modifications just discussed, the following situations should be studied with the idea of improving various standards if and whenever revisions appear to be advisable.

A. As is generally appreciated, an ambient temperature of 40 degrees prevails only over relatively short periods in the majority of applications. This, together with the fact that deterioration of insulation is a function of both temperature and time, provides a margin of safety in all but a few exceptional applications. This and the further fact that machines and apparatus carry full loads continuously only in rare applications, makes it doubtful that the extra 10 degrees provided by the 40-degree rise of general-purpose motors is really warranted. However, a study of figures 2 and 3 indicates that up to 100 horsepower, the temperature rises of commercial machines would in general be below 40 degrees even with a 50-degree standard because the size of the machine is appreciably influenced by other considerations, such as starting and pull-out torques, power-factor values, commutation, etc. In other words, even granting that the additional margin provided by the 40-degree rating is not really necessary, no marked economies could be obtained by the 50-degree standard with the present established standards of performance relating to factors other than temperature. Therefore, at this time there seems to be no valid reason for disturbing the 40-degree standard for general-purpose motors up to about 100 horsepower; in fact, there is considerable merit, under conditions indicated in figures 2 and 3, in adhering to the 40-degree standard because it has the advantage of letting the user know that he can make use of the extra margin whenever conditions make this desirable. Thus the established standard of 40 degrees will lead to economies in some cases rather than interfere with maximum economy. The dotted lines in figures 2 and 3 show the temperature rises which might be obtained in actual practice with satisfactory all-round standards of performance if a 50-degree standard were established for 100 horsepower and above. From the figures it will be seen that slight economies might be obtained by limiting the 40-degree standard to the range below 100 horsepower.

Figure 4, applying to transformers, indicates that owing to the necessity for obtaining satisfactory regulation, the actual temperature rises in transformers are usually below the established standard of 55 degrees for ratings up to about 15 kva. Therefore, applying the same line of reasoning as given for general-purpose motors, a lower standard of temperature rise could be established for the lower ratings. On the other hand, since transformers are usually applied by specialists in electrical engineering who are in general familiar with the conditions indicated in figure 4, there is little need for making a change in well-established standards of rating. Furthermore, the increasing application of thermal devices indicating temperature rises in service may eventually assure the fullest and most efficient utilization of transformer capaci ties. This situation is covered fully in a paper previously presented by Messrs Putman and Dann.[12]

B. Careful tests have demonstrated tha if the oil in transformers is protected fror contact with air so that it will not absorl any oxygen, there is practically no deteriora tion in insulating materials at the limits o temperature suggested by the AIEE an ASA standards. Similarly, there is con siderable evidence that the insulation o hydrogen-cooled machines is subject to les deterioration than machines cooled by ai While for the present these advances ma be taken advantage of by the methods o applying these machines and transformer with neutral atmosphere, serious considera tion eventually should be given to the recog nition of such improved operation in th AIEE and other standards. A carefu study of the conditions necessary to assur this improved operation and their inclusio in the standards will be advisable.

C. Although there was merit in a singl standard for the measurement of tempera ture by thermometer specified in many o the ASA and NEMA rules as long as th windings and other parts of machines wer readily accessible, recent trends make i necessary to recognize in these standard the measurement of temperature by th resistance method, at least under some con ditions. This change is brought about b the fact that an increased number of ma chines is being built in a way which make windings and other hot spots inaccessible Even where these parts may be reached b removing covers, too much time usually i consumed in doing so to permit accurat readings. In other apparatus, such a refrigerators and air-conditioning equip ment, the motors are enclosed in such a wa as to make thermometer readings entirel impossible or useless. In this connection it should also be considered that in othe countries the resistance method is generall preferred for motors, transformers, an generators for industrial and power pur poses. More recently, at the insistence o the American representatives and others the resistance method has been adopted b the IEC as the governing method for us with the windings of traction motors This is another reason for giving mor recognition to this method in many of ou standards, although its immediate adoptio in all cases and particularly in those wher thermometer readings are possible, ma not be found advisable.[8]

A proposition has been made recently especially in a paper by Rutherford,[13] tha a motor be rated on the starting torqu per horsepower, accelerating torque pe horsepower, and starting torque per am pere locked-rotor current. These factor should be taken into consideration alon with the temperature rise of the moto at the rated horsepower. In anothe paper,[11] a system is proposed whereb the rating is based on a combination o temperature and starting current, th latter serving as an approximate measur of the torques. These methods are full described in the papers and are mentione

ere principally for the sake of complete-ess.

Although the action suggested in this aper is by no means revolutionary, it vould nevertheless tend to improve the xisting structure of ratings appreciably y making it more uniform and putting t on a sound economic basis. It is vident that any standard must be kept p-to-date and that progress will be etarded materially and certain econo-nies will not be accomplished if there is oo much delay in recognizing economi-ally and technically sound developments hrough suitable modifications and sup-lements to our standards. A careful tudy of the points mentioned here and lso others given elsewhere relating to tandards would therefore seem ap-ropriate, and it would seem that a re-vision of AIEE Standard No. 1 in par-icular is in order. Although mention as been made here of specific types of pparatus for the purpose of illustration, his should not be construed as suggesting hat any specific type of apparatus be overed by Standard No. 1. This stand-rd should in the future, as in the past, leal only with general principles and erve as a guide in establishing other tandards for specific lines of machines nd apparatus.

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Discussion

E. F. Dissmeyer (The Commonwealth and Southern Corporation, Jackson, Mich.): No doubt there is justification for revision of certain present standards; however, extreme caution should be used in making any changes. It is unfortunate that there is not sufficient data available showing the relationship between operating temperature and the thermal life of various types of insulation. Consequently, the results of increasing the operating temperature of equipment cannot be predicted and our experiences may be costly unless caution is used in revising present standards.

Experience indicates that the tempera-ture rise of a machine is indicative of its reliability and, for certain equipment appli-cations, the permissible temperature rise should possibly be reduced. This is es-pecially true for large machines where the loss of a machine usually results in serious consequences. Even under modern con-ditions, design errors and contingencies of manufacture and operation make it ad-visable to provide a margin of safety in the thermal design of a machine.

The reliability angle of this problem could be minimized by the development of suitable nondestructive tests which would make it possible to anticipate insulation failures. If it were possible to schedule replacement or rewinding of equipment, it would probably be possible to obtain an economic basis for increasing the permissible temperature rise of equipment.

R. E. Hellmund: The appeal made in the discussion of my paper for the use of ex-treme caution in changing standards is well justified and is in accord with statements in the paper itself. However, caution must not be carried to an extreme contrary to sound economic principles. It obviously would not be economical to establish rating standards which would give safe operation under any service conceivable; this would result in enormous waste in the majority of applications. The basic standard should be such that it results in safe and reliable operation in the large majority (possibly 75 to 85 per cent) of all applications, with a definite understanding that the remaining cases will be given special consideration in some way or other. The subcommittee of the AIEE standards committee, on basic principles for rating of electrical machines and apparatus, expects to under-take very extensive studies and to sponsor further papers and discussions on such phases as ambient temperature, life of insulation, allowances between hot-spot temperatures and temperatures measured by thermometer and resistance, etc.

I am at present of the opinion that the data so far available do not indicate the advisability of departing appreciably from present basic standards such as the tempera-ture rise of 50 degrees by thermometer now specified in AIEE Standard No. 1 for class *A* insulation. In some cases this might be supplemented or replaced by the practically equivalent rise of 60 degrees by resistance. The practice of having more liberal com-mercial standards with temperature rises of 40 degrees by thermometer (possibly supplemented or replaced in some cases by a value of 50 degrees by the resistance method) probably should be continued to take care of cases where service conditions are more severe or where a greater margin of safety is desirable for some reason or other. Similarly, it may not be found advisable to change existing values materially for con-ventional class *B* applications, or those values which have proved satisfactory for transportation work and applications with similar economic and service conditions. However, final recommendations on all this should be withheld until some further investigational work which is now under way has been completed. During this period of study, it would be very helpful if operating engineers would assist to a greater extent in supplying pertinent and reliable data gathered from actual service experi-ence.

Loading Transformers by Copper Temperature

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THE PURPOSE of this paper is to describe a new practical method of utilizing latent short-time overload capacity to the fullest extent, dependent upon actual copper temperature and automatically taking into account the factors that affect the life of a transformer.

It seldom happens in service that all the conditions established for the rating of a transformer exist concurrently and continuously and it is seldom the case that its maximum capacity is actually used for any great length of time. For these reasons it is realized that under many conditions transformers have an unused latent load capacity over and above their ratings, particularly for short time periods. That latent capacity depends upon the temperature to which the insulation can be safely exposed and how long it is exposed to it. Obviously ambient temperature plays its part for copper temperatures rise and fall with ambient temperature and the latent capacity is greater when the ambient temperature is low.

Users of transformers are naturally interested in making use of this latent overload capacity if a safe way can be devised for doing so. It is desirable for at least two reasons: first, the short-time overload capacity may be needed in emergencies due to the failure of some part of the system; second, short-time overload capacity can be used to carry peak loads and in that way reduce the average size of transformer required for a given load. The proper utilization of short-time overload capacity is therefore desirable in order to reduce costs and improve quality of service.

But how can the short-time overload capacity of transformers be utilized in practical system operation? The customer's load usually determines the loading of any particular unit and as a rule loads cannot be varied at will. In the case of distribution transformers, considerable effort on the part of the utility company is required in the direction of load surveys to determine the extent to which transformers are loaded. In the case of power transformers, graphic records of actual loads may be available, and in some cases hot-spot temperatures, but ordinary overload protection schemes will relay out a power transformer long before its maximum short-time overload capacity has been reached.

There are two parts to the problem of making use of latent overload capacity: first, the safe limits of temperature for different loads and time periods must be known; second, a practical method of operating up to but not beyond these temperature limits must be available.

Operating Temperature and Useful Life

Attempts have been made to establish a system of relationships between operating temperature and the useful life of a transformer. While they have been based upon data which are accepted as reliable under the conditions of test and upon reasoning which is not general[...] disputed, they are so far largely of ac[...] demic interest as reports of progre[...] rather than as tools of complete practic[...] value in making use of the latent overloa[...] capacity of a transformer. Obviousl[...] the positive and direct way of determi[...] ing accurately the relation between li[...] and temperature would be to provide [...] large number of transformers and loa[...] them variously for long periods of tim[...] That is clearly impracticable on a lar[...] scale, but the tests reported in this pap[...] reflect this positive and direct method [...] obtaining such results.

In 1930, V. M. Montsinger presente[...] a comprehensive paper[1] before the Inst[...] tute in which was stated the theory th[...] the rate of deterioration of insulation [...] oil is doubled for each increase in temper[...] ture of eight degrees centigrade an[...] conversely, is reduced one-half for eac[...] decrease of eight degrees centigrad[...] This theory is widely accepted as trus[...] worthy. Based upon this theory and [...] number of calculations of temperatu[...] under various conditions of loading, M[...] Montsinger built up a relationship b[...] tween temperature and the useful life of [...] transformer in years. One conclusio[...] reached was that a self-cooled transforme[...] having an average temperature rise [...] 55 degrees centigrade and a maximu[...] temperature of 105 degrees centigrad[...] actually operating at full load contin[...] ously in an ambient temperature [...] 40 degrees centigrade, would have [...] useful life of about seven years. Tes[...] made on insulating materials in open [...] formed the starting point for his curve [...] relationships and for this conclusion.

In 1934, L. C. Nichols presented [...] paper[2] in which a method of relatin[...] temperature and life was proposed. Th[...]

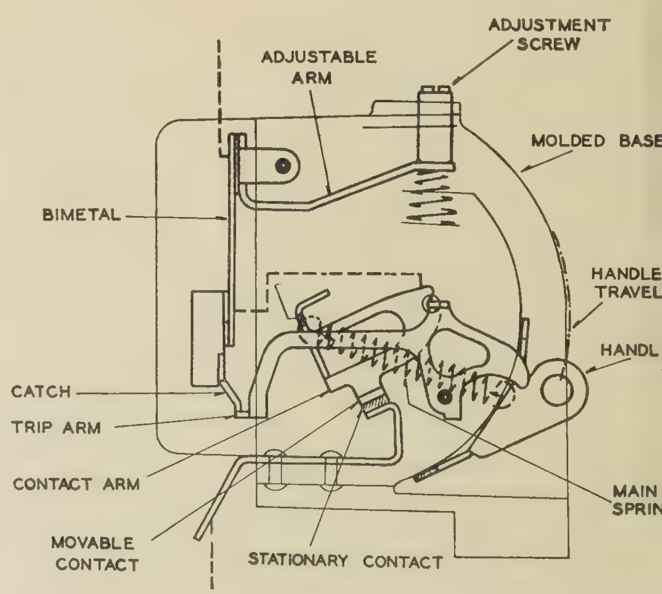

Figure 1. Schematic diagram showing the mechanism of the type "FR" thermal relay and the path of the load current through the contacts and the bimetal by dot-and-dash line

Paper number 38-122, recommended by the AIEE committees on electrical machinery and protective devices, and presented at the AIEE Southern District meeting, Miami, Fla., November 28–30, 1938, and at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted August 31, 1938; made available for preprinting October 20, 1938.

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1. For all numbered references, see list at end of paper.

starting point for this system was based upon a five-year test made on insulating materials in open oil at 90 degrees centigrade. This system indicated a useful life of $1^1/_3$ years for a self-cooled transformer having an average temperature rise of 55 degrees centigrade, maximum temperature of 105 degrees centigrade, and operating continuously at full load.

Systems such as these can be built up acceptably so far as relationships between different operating conditions are concerned but they are vulnerable in that they finally must be tied into fundamental time-deterioration data of unquestioned reliability obtained either from tests which correctly represent operating conditions or else from actual operating experience. The difference between 7 years and $1^1/_3$ years of useful life derived from the two systems mentioned is an illustration of the point. A comparatively small change in the starting point of either system would result in considerable change in indicated periods of useful life.

The recommendations for short-time overloading prepared by the AIEE transformer subcommittee[3] and the ASA sectional committee on transformers are based upon the eight-degree rule and upon actual experience in service, with perhaps a rather liberal interpretation of that experience. It is assumed, a little questionably perhaps, that certain relationships between maximum temperature limits and time have been demonstrated as correct because they have been used satisfactorily for many years for purposes of standardization. These time-temperature relationships are:

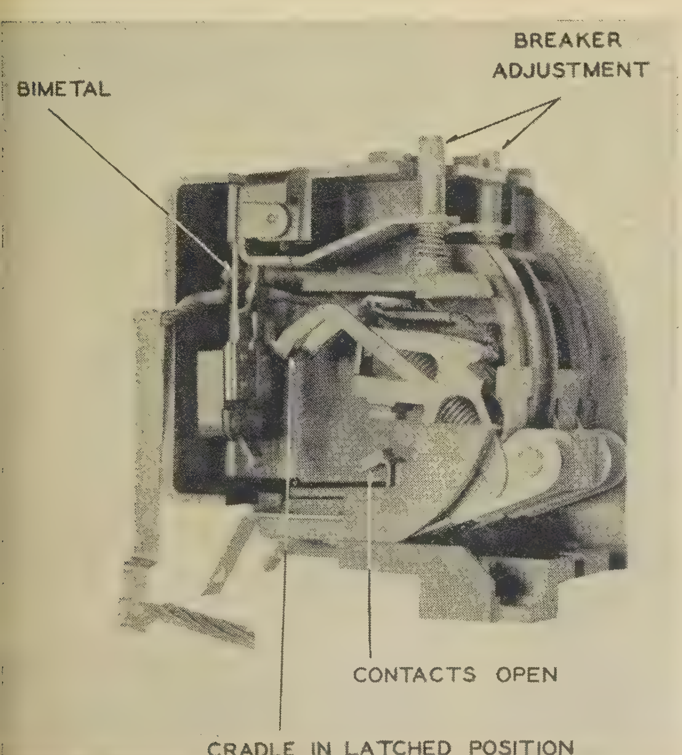
250 degrees centigrade for five seconds—used as a limit for short-circuit conditions

160 degrees centigrade for one minute—used for grounding transformers

95 degrees centigrade for continuous operation—this is the hottest-spot winding temperature of a 55-degree transformer operating in 30 degrees centigrade ambient

A curve drawn through these three points gives a complete relationship between temperature and useful life. It may be said of each of the three points that they perhaps have not been actually and consistently reached in average operating experience, and for that reason, they do not accurately represent actual experience. If so, they merely err on the conservative side.

Figure 2. The type "FR" copper temperature relay showing the mechanism in the tripped position



Connected in series with the transformer winding, either directly or through a current transformer, it provides the means for overload relaying directly by copper temperature, thus permitting overloads of any size or duration until the limit of copper temperature is reached

The type "*TR*" relay, a somewhat more elaborate device, gives an advance warning light signal at a copper temperature about 30 degrees centigrade under the maximum safe temperature

Operation With Inert Gas

Temperature-deterioration data so far published have all been acquired in tests of materials in open oil with the oxygen of the air contributing a certain measure of deterioration to the materials tested. In contrast with such tests, elaborate long-time tests of coils and of the commonly used insulating materials have been made in oil protected against the harmful effects of air by means of "inertaire." As was to be expected, these tests showed that insulating materials suffer appreciably less deterioration and have longer useful life for a given temperature. In other words, they show that insulating materials in oil protected against contact with air will withstand appreciably higher temperatures than in open oil. The conclusions drawn from these carefully made tests indicate clearly that there is practically no deterioration of insulating materials at the limits of temperature suggested by the Institute and the American Standards Association.

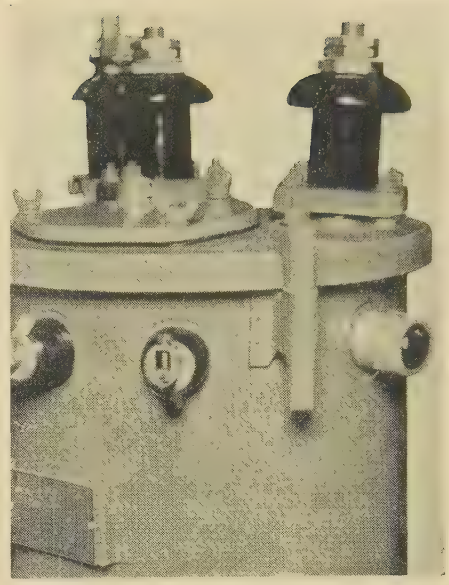


Figure 3. The indicating lamp as used with distribution transformers

When the copper temperature reaches the AIEE limit of temperature, this warning signal comes on. Even though the load decreases, the lamp remains lighted until it is reset, thereby indicating that at some time the load on the transformer reached its maximum safe value

The practical benefits accomplished with inert gas in maintaining the oil in the best possible condition and in reducing the rate of deterioration of the insulation have come to be very generally understood and appreciated. The trend in transformer practice has been toward operation with inert-gas protection or, as the next best thing, toward the use of gas-tight cases which limit the amount of air in contact with the oil and prevent the addition of more air. The best practice, except for inert-gas protection, is to make use of rugged gas-tight cases that do not allow the inbreathing of fresh air, with enough air space to limit the internal pressure to a reasonable value at the maximum operating temperature. In making cases gas-tight, the gasketing of joints between covers and tanks and at bushings and

handhole or manhole covers has always been a serious problem. That problem seems to be solved successfully with a new gasketing material called "corprene."

Today designers of transformers are thinking not of name-plate ratings alone but are giving careful consideration to the design of bushings, leads, tap-changers and the like, to see that they will not constitute limits which would prevent the complete utilization of whatever latent overload capacity is otherwise available.

The useful results of making transformers gas tight are perhaps not generally recognized. The practice has an appreciable effect in reducing the deterioration of oil and insulation and in giving the transformer longer life. It contributes a measure of increased latent overload capacity. It has been proved that considerable short-time overload capacity is available in distribution transformers of gas-tight construction and that it can be utilized with the method of loading by copper temperature about to be described.

Loading by Copper Temperature

The term "loading by copper temperature" has been applied to the practical utilization of short-time overload capacity which has been developed and applied to distribution transformers of the "*CSP*" type, and more recently to "*CSP*" power units. Briefly, the method consists of relaying directly by copper temperature in such a way that the transformer will carry any useful overload until its maximum safe temperature is reached before it automatically disconnects the load. Under short-circuit conditions, however, it will disconnect the load immediately.

The dividing line between useful overload range and short-circuit range is largely an arbitrary one which can be changed by adjustment of the relay. Experience shows that for distribution transformers the useful overload range should extend up to 5 or 6 times normal load, while for power transformers a range up to $3^1/_2$ to 4 times normal load proves satisfactory.

The relay which accomplishes these desirable results consists of a suitable contact mechanism actuated by a bimetal or thermostatic element which is immersed in the same oil as the winding and which carries the same current or a proportional current obtained with a current transformer. Figures 1 and 2 show a schematic diagram and a picture of the simplest type of this relay.

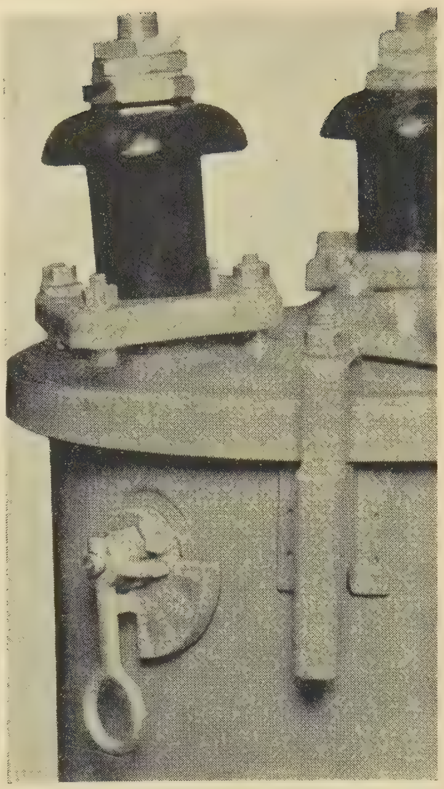


Figure 4. Operating handle of the relay

Used with the distribution transformer to reset the indicating lamp and breaker after tripping. The handle can also be used to open and close the breaker, a convenience during the installation of the transformer

It is shown in the appendix that the temperature of the bimetal can be made to follow the copper temperature in such a way that it always arrives at the tripping temperature whenever the copper of the winding reaches the maximum safe temperature. It is also shown that, by proper correlation of the design of the bimetal with the winding gradient, this will be true regardless of the ambient temperature. This statement applies only to the useful overload range, for the breaker trips immediately in the short-circuit range without waiting for the copper to arrive at its maximum safe temperature, as the analysis of the appendix clearly shows. The higher the short-circuit current the faster will be the tripping. The point where this immediate tripping begins can be changed at will by adjusting the relay.

The type "*TR*" relay, which is somewhat more elaborate than the simple "*FR*" type shown in figures 1 and 2, embodies an arrangement whereby a signal gives an advance warning of an approaching high-temperature condition which might cause an outage if it is allowed to continue.

In the small "*CSP*" distribution transformer, the relay element operates an internally mounted circuit breaker d rectly and the warning signal is an ind cating lamp (see figure 3). In the ca of the power transformer, the relay connected in the control circuit of th main circuit breaker and the sam warning signal is used.

Advantages of "Loading by Copper Temperature"

The simple arrangements describ above can be very helpful in syste

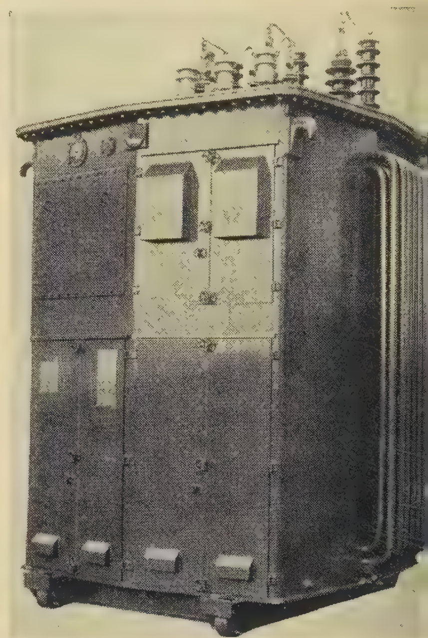


Figure 5. A 1,000-kva "CSP" pow transformer

This transformer is provided with a therm relay for operation by copper temperatur In an emergency, this transformer carried 19 per cent load for two hours, safely and witho a service interruption—a good example of th practical use of latent short-time overlo capacity

operation for minimum cost and max mum service continuity. For exampl consider their application to distributio transformers. Smaller units can be safel selected initially because it is not nece sary to provide for future growth sin there is little possibility of burnou As loads gradually build up on certa transformers to the point where lar units are required, the warning signa will automatically indicate the situati and those particular transformers can replaced with larger sizes.

Similar advantages are realized wh "operation by copper temperature" applied to power transformer units. C one system where a number of "*CS* power units similar to figure 5 were

operation, it was necessary because of emergency repair work to maintain service, to place a 1,900-kva load on a 1,000-kva three-phase transformer. This condition existed for two hours before the load could be reduced. Toward the end of that period the warning lamp signal operated indicating an approach to a dangerous temperature, but the point of breaker operation was not reached. The load temperature curves for this unit indicated that it would be expected to carry such an overload following full-load temperatures for somewhat over two hours before tripping out on copper temperature. This illustrates clearly the value of operation by copper temperature in eliminating unnecessary service interruptions.

In establishing limits of copper temperature for the "*CSP*" transformer, the signal light is set to operate at about 95 degrees centigrade average copper temperature by resistance. This is the limit recognized in the AIEE Standards. It is the purpose to set the tripping point of the breaker as high as possible without burning out the windings in order to eliminate all but absolutely necessary interruptions due either to short circuit or to really dangerously high temperatures. For the same reason it is felt desirable to design the bimetal so that it will permit higher temperatures for short periods of time than for long periods rather than to trip at the same maximum temperature regardless of the duration. This accounts for the "hump" at about one hour in the curves of winding temperature in figure 7. To obtain this characteristic a somewhat lower bimetal resistance is used than is given by equation 7 in appendix I.

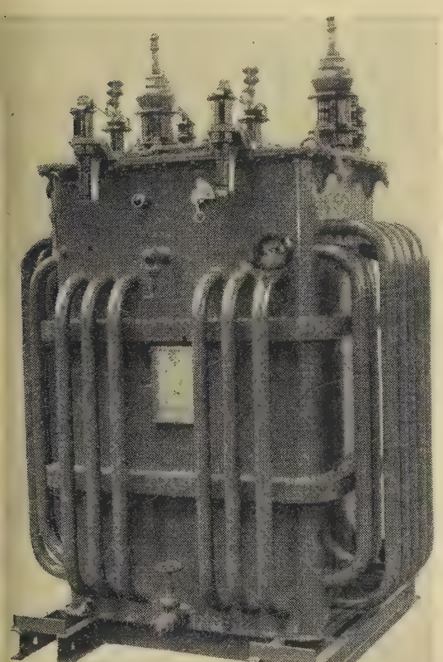


Figure 6. A 333-kva 7,200-volt single-phase transformer equipped with a thermal relay for operation by copper temperature

The maximum average temperatures reached for different overloads on a five-kva "*CSP*" transformer are shown in figure 7, together with the corresponding overloads. It will be found that for overloads of several hours duration the maximum temperature permitted is about 120 degrees centigrade, while at approximately 328 per cent load for $1^1/_4$ hours the maximum temperature is 145 degrees centigrade. For still larger loads and up to the short-circuit range, maximum temperatures are less.

Tests of the New System

If the breaker is set so that a maximum average copper temperature of 145 degrees centigrade is reached under some conditions, it would be natural to inquire if such a temperature would not damage the winding and how many times a winding could be subjected to such a temperature before failure.

To answer these questions, a program of temperature-cycle tests was started on four five-kva "*CSP*" transformers on June 1, 1936, and is still in progress. These transformers have been operated back to back in two groups, the units of the first group being designated as *A* and *B*, and in the second group as *C* and *D*. A summary of the tests is given in table I.

The procedure has been as follows: Starting at room temperature, a load of 278 per cent was applied to units *A* and *B*, and 350 per cent to units *C* and *D*. These loads were maintained until the breakers tripped on copper temperature in each case. Figure 7, which gives the characteristics of these particular transformers, shows that the 275 per cent load would trip the breaker in about two hours at average copper temperature by resistance of about 132 degrees centigrade. Similarly, the 350 per cent load would be carried about one hour and would result in an average copper temperature of approximately 145 degrees centigrade at the tripping point. These values assume an ambient temperature of 25 degrees centigrade, while during the tests the actual ambient temperature varied roughly from 17 degrees centigrade to 32 degrees centigrade during the summer and winter seasons. The durations of the overloads were correspondingly affected, but the attained copper temperatures were not measurably affected. These cycles of overload and trip-out were repeated continuously during the complete runs.

At the end of the first run, transformers *A* and *B* had been subjected to 100 cycles of operation at 275 per cent load to the tripping point, and trans-

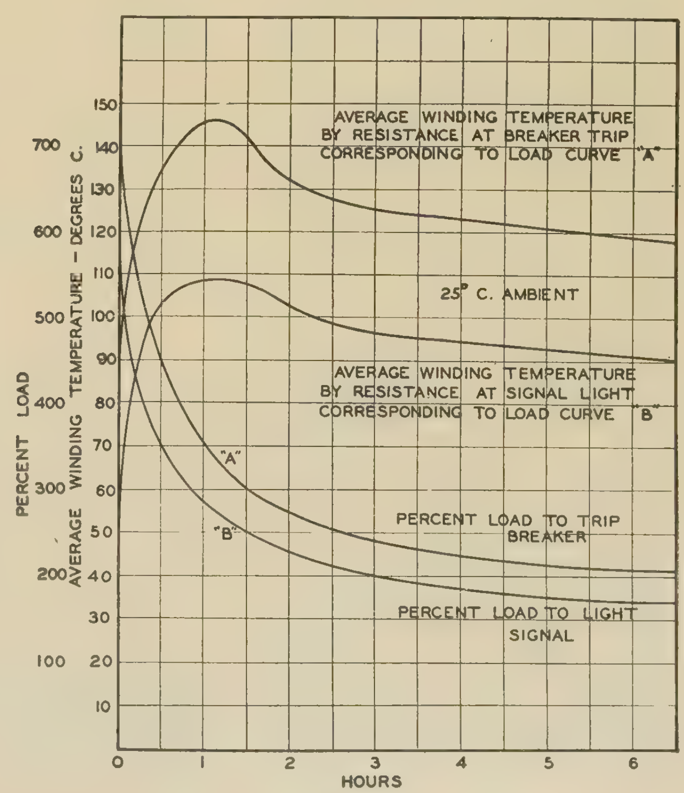


Figure 7. Typical short-time overloads for a five-kva distribution transformer

Curve *B* shows the load required to light the signal light for various time periods and curve *A* correspondingly shows the loads required to trip the breaker

Table I. Summary of the 660 Cycles of Short-Time Overload Operation of Five-Kva "CSP" Distribution Transformers

Run Number	Transformers	Load (Per Cent)	Cumulative Number of Cycles	Attained Maximum Copper Temperature by Resistance (Degrees Centigrade)	Average Duration of Each Overload (Minutes)
1	*A* and *B*	275	100	132	120
2	*A* and *B*	390	192	142	48
3	*A* and *B*	390	500	142	48
4	*A* and *B*	390	660	142	48
1	*C* and *D*	350	100	145	58
2	*C* and *D*	450	192	135	34
3	*C* and *D*	450	500	135	34
4	*C* and *D*	450	660	135	34

formers *C* and *D* had been subjected to 100 cycles at 350 per cent load. At this point the regular AIEE dielectric tests were applied to the transformers, the dielectric strength of the oil was measured, and the breaker calibrations checked. The transformers passed their dielectric tests, the dielectric strength of the oil averaged about 30 kv, and breaker calibrations were found to be within the proper bands.

In starting the second run it was decided to increase the severity of the tests and, accordingly, the loads were increased to 390 per cent and 450 per cent, respectively. After 92 additional cycles, standard AIEE dielectric tests were successfully applied, the dielectric strength of the oil was found to average 35 kv, and a check of the breaker calibration showed that it was still in the band.

Following these tests of 192 complete cycles of high-temperature operation, an ignitron short-circuit test was applied to two units to see if any mechanical weakness in the insulation could be developed. The units were excited at double voltage and frequency in order to overstress the insulation electrically at the same time that mechanical stresses were applied. The ignitron timer was adjusted to apply five short circuits spaced six cycles apart and repeated every 15 seconds for one minute. By this test very definite and forceful vibrations were set up in the transformer windings. To determine whether the insulation had suffered mechanical damage, impulse tests were successfully applied, followed by double-voltage excitation and ratio tests.

Since no signs of weakness were detected, it was decided to continue the cyclic loading tests up to a total of 500, which was reached May 25, 1938. Breaker calibrations were checked and found to be within the original band. Dielectric strength of the oil was measured on the four units and found to be 33.7 kv, 36 kv, 34.3 kv, and 30.7 kv. The oil was reported dark red wine color and cloudy, but oil ducts were clear and temperature measurements at this point checked the original measurements. Impulse tests were repeated successfully, also the AIEE dielectric tests, including a one-minute induced test at 400 per cent of normal voltage.

The cyclic loading tests were again continued and by June 15, 1939, a total of 867 cycles will have been completed. The tests were started June 1, 1936, and, as stated before, have been run almost continuously ever since.

Consequences of the Tests

Frankly, the results are surprising, in view of life tests on samples of insulating materials which have been made. It was expected that failure would take place after a comparatively few cycles of such operation. The tests seem to indicate the possibility of higher short-time temperature limits than have been believed possible. They also raise a question as to the reliability of methods heretofore used for determining max
mum temperature limits. As has bee
stated, limiting temperatures have ger
erally been established by testing in
dividual samples of insulating materia
over extended periods in oil under variou
conditions and measuring the deprecia
tion in mechanical strength.

In view of these results, it may be i
order to suggest that the proper com
mittees of the AIEE review the who
subject of maximum temperature limi
of insulating materials.

In conclusion, it can be said that tran
formers designed for operation by coppe
temperature and meeting, at least ap
proximately, the gradient conditions se
forth in equation 13 of the appendix, d
possess desirable short-time overloa
capacity which can be used for greate
economy and reliability in system opera
tion.

The simple method of operation b
copper temperature described here seem
well adapted to transformers of all sizes
Present equipment has no limitations s
far as size or voltage class are con
cerned. Operation by copper tempera
ture does not preclude the use of relayin
on ground faults, or reverse power, or c
conventional reclosing practice.

The advantages of operation by coppe
temperature are so obvious that on
might predict its almost universal us
in the not distant future.

Appendix—Characteristics of Transformer Bimetal Elemen Under Oil

Let

T_o = oil temperature or initial tempera ture of the bimetal

T_t = trip temperature of the bimetal

T = temperature attained by the b metal at any time t

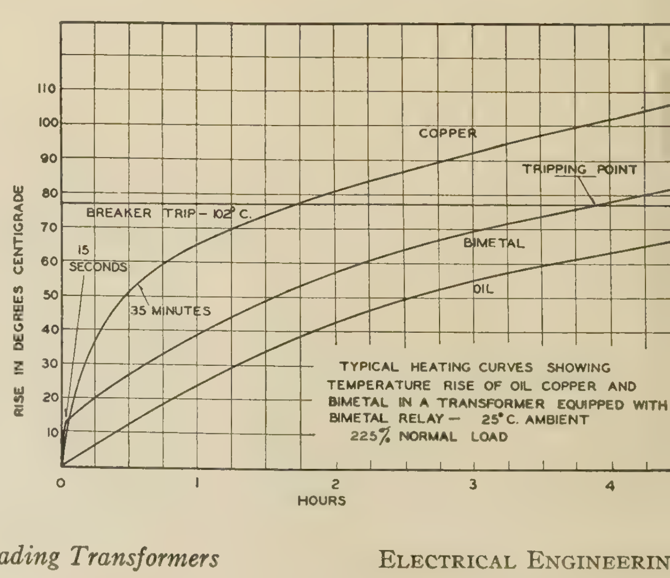


Figure 8. Typical heating curves for a "CSP" distribution transformer provided with a thermal relay

T_{oL} = oil temperature corresponding to load L
T_{RoL} = oil temperature rise corresponding to T_{oL}
I = current through the bimetal
I_n = normal full load current
L = steady-state load required to give the maximum desired hot-spot copper temperature
W = watts input to the bimetal
R = total resistance of the bimetal
N = number of times full-load current required to produce immediate tripping
p = specific heat of the bimetal—watt seconds per gram per degree centigrade
m = mass of bimetal in grams
q = coefficient of heat convection of the bimetal in watts per square centimeter per degree centigrade
a = area of the bimetal in square centimeters
Q = aq = watts dissipated from bimetal per degree centigrade gradient
t = time period

If a current is suddenly caused to circulate through a bimetal immersed in oil at temperature T_o, the I^2R loss will partly heat the bimetal and partly be lost to the oil. Thus

$$Wt = pm(T - T_o) + \int_0^t (T-T_o)aqdt \quad (1)$$

Differentiating with respect to t gives:

$$\frac{dT}{dt} + \frac{aq}{pm}T = \left(\frac{W}{pm} + \frac{aq}{pm}T_o\right) \quad (2)$$

which is the differential equation for the temperature of the bimetal as a function of time. In solving this equation for an actual transformer it can be assumed that the oil temperature is constant since the bimetal transient is very rapid and will have disappeared entirely before there is any substantial change in the oil temperature.

The solution of this equation is easily shown to be

$$T = T_o + \frac{W}{aq}\left(1 - \epsilon^{-\frac{aq}{pm}t}\right) \quad (3)$$

In figure 8, heating curves are shown for an actual transformer. The sudden rise in the temperature of the bimetal near $t = 0$ is the transient given by equation 3.

As soon as the transient is over, that is, for large values of t, the temperature of the bimetal is given by

$$T = T_o + \frac{W}{aq} \quad (4)$$

and further increase in the bimetal temperature results only from the gradual rise in the oil temperature as can be seen from the curves of figure 9. It is apparent, therefore, that considerable time will ordinarily elapse before the bimetal attains the tripping temperature or the temperature at which it unlatches the breaker—the time being determined by that required for the oil to heat up.

However, it is apparent from equations 3 and 4 that if W is very large, as in the case of a short circuit, the temperature which the bimetal would attain almost immediately from equation 3 could be high enough to unlatch the breaker without waiting for the oil to heat up. Or

$$T_o + \frac{W}{aq} \text{ could equal } T_t$$

if W were sufficiently great.

The load or current at which this immediate tripping begins can be established arbitrarily by the proper selection of the bimetal resistance. The range during which immediate tripping takes place is designated here as the short-circuit range, while the range during which the tripping time is dependent on the heating of the oil is designated as the useful overload range.

Equation 3 is necessary for the calculation of the tripping characteristic of the bimetal relay in the short-circuit range.

Experience has taught that the useful overload range for distribution transformers should extend up to five to seven times normal current since transformers may be subjected to currents of this order due to motor starting or similar short-time overloads. In other words, N should be between five and seven. To bring about immediate tripping at any particular value of N, the bimetal resistance can be determined as follows:

$$T_t = T_o + \frac{N^2I_n^2R}{aq_1} \quad (5)$$

But under steady-state conditions the trip temperature must be reached at load L. Hence

$$T_t = T_{oL} + \frac{L^2I_n^2R}{aq_2} \quad (6)$$

q is not quite constant because the oil convection increases with increased oil temperature. q_1 is therefore used in equation 5 and is determined for T_o while q_2 in equation 6 is determined for oil temperature T_{oL}.

Equating (5) and (6) and solving for R gives

$$R = \frac{Q_1T_{RoL}}{I_n^2\left(N^2 - \frac{q_1}{q_2}L^2\right)} \quad (7)$$

where

$Q_1 = aq_1$

which is the bimetal resistance required to give a useful overload range up to load N and immediate tripping above N.

A highly desirable characteristic of the bimetal relay is that it compensates automatically for changes in ambient temperature permitting higher loads at low temperatures and less load at high temperatures in accordance with the thermal capability of the transformer. Obviously this comes about if the bimetal trips the breaker at the same maximum copper temperature regardless of ambient temperature. To bring this about the winding gradient must fulfill a condition determined as follows:

As the permissible steady-state load L changes with ambient temperature, if the bimetal trip temperature is to be reached at the same maximum copper temperature, then any increase in the bimetal gradient (W/aq) with increasing load must be offset by a corresponding decrease in the oil temperature with increasing load—or

$$\frac{dT_{oL}}{dL} = \frac{-dW}{dLaq} \quad (8)$$

But

$$W = L^2I_n^2R \quad (9)$$

$$\frac{dT_{oL}}{dL} = \frac{-2LI_n^2R}{aq_2} \quad (10)$$

But the transformer characteristics determine the relation between oil temperature and the maximum permissible steady load and varying ambient from which dT_{oL}/dL must be evaluated.

In general

$$T_{oL} = T_c - KL^x \quad (11)$$

where

T_c is the copper temperature corresponding to load L
KL^x = gradient

Usually x ranges from 1.5 to 2, but the heating curves of the transformer determine the exact value to use.

Differentiating (11) at constant T_c gives

$$\frac{dT_{oL}}{dL} = -xKL^{x-1} \text{ or } = -2KL \text{ (for } x = 2) \quad (12)$$

Equating (10) and (12) and solving for K gives

$$K = \frac{I_n^2R}{aq_2} \quad (13)$$

which is the gradient coefficient required for ambient temperature compensation, the value of R having been obtained from equation 7.

The gradient required by equation 13 will be found quite low, and if it cannot be obtained it may be desirable to change the bimetal resistance to give ambient temperature compensation with the K which can be obtained. In this case

$$R = \frac{KQ_2}{I_n^2} \quad (14)$$

and the maximum overload before immediate tripping would be

$$N = \sqrt{\frac{Q_1}{Q_2}\left(L^2 + \frac{T_{RoL}}{K}\right)} \quad (15)$$

which is obtained by substituting (14) in (7) and solving for N.

References

1. Loading Transformers by Temperature, V. M. Montsinger. AIEE Transactions, volume 49, April 1930, page 776.

2. Effect of Overloads on Transformer Life, L. C. Nichols. AIEE Transactions, volume 53, 1934, page 1616.

3. Operating Transformers by Temperature, W. M. Dann. AIEE Transactions, volume 49, April 1930, page 793.

Discussion

F. M. Starr (General Electric Company, Schenectady, N. Y.): This paper is of considerable interest and importance because it presents new data on the ability of transformers, particularly of the distribution class to carry heavy overloads. Considerable has been written in recent years as to the proper thermal overloads which should be tolerated in transformers. Conclusions reached vary widely in degree, but in principle they are all in agreement that repeated overloads reduce transformer life and that there is a threshold of loading beyond which the reduction in transformer life is so pronounced as to make such overloads extremely uneconomical. This point of view limits its economics pretty largely to the life and cost of a transformer.

The distribution engineer must necessarily have a broader point of view since his problem is somewhat greater than getting the maximum possible usage out of his equipment. It is his job to design a distribution system to deliver a kilowatt-hour to the consumer within specified criteria of voltage regulation and service continuity at the lowest possible cost. In making his broad economic analysis to this end he finds that energy losses and voltage drop in the transformer and perhaps even revenue are just as important factors in determining the loading of a transformer as its thermal capacity. For example it has been found in certain localities that the most economical design of secondary networks, considering voltage regulation and energy losses as contributing factors, results in transformer loading of only 75 per cent of name-plate capacity.

The reaction of the distribution engineer to the conclusions in this paper is likely to be that he is getting and paying for thermal capacity in transformers that he cannot possibly use because of excessive voltage drops or transformer losses. He might justifiably ask, "Why not sacrifice some of this thermal capacity and give me a lower cost transformer or a lower impedance transformer." Because of basic design limitations it is difficult to obtain what might be called an ideally balanced design of transformer for the economic distribution system. There has been considerable progress in this direction, however. It seems to me important and significant that if a more perfectly balanced design of transformer embodying maximum economy as well as the desirable operating characteristics is to be attained in the future, distribution engineers must not impair such progress by specifying unusable and unnecessary thermal capacities which have characterized some designs of the past.

J. H. Christensen and **J. P. Hamilton** (both of Tennessee Valley Authority, Wilson Dam, Ala.): The temperature-cycle tests described by the authors were made to determine whether or not a maximum average copper temperature of 145 degrees centigrade would harm the winding insulation. The transformers subjected to this test were of the five-kva "*CSP*" type. The authors have shown that these tests, which were made continuously over a period of two years without an insulation failure, indicate that the present maximum copper temperature of 105 degrees centigrade may be entirely too low. If so, transformers of higher ratings could be designed with only slightly increased costs, provided the relays under discussion give satisfactory operation under field conditions.

In this connection, a question arises concerning the practical application of this relay, namely, what provision is proposed to facilitate the adjustment, maintenance, or replacement of these units in large power transformers.

The outstanding feature of this work is the novel means used to obtain, with safety, the fullest utilization of conventional insulation in transformers. It is interesting to note the similarity in the design and application of this feature to that of the type "H" Sentinel breaker used for small-motor overload protection.

W. C. Sealey (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The tests described in this paper show that a modern transformer will stand a great amount of abuse without actually failing. The previous tests and experience on the deterioration of insulating materials due to time and temperature are not challenged by the results of the tests. Very high overloads have been carried and the transformer has been able to operate successfully after being subjected to the high temperatures obtained. However, it has been demonstrated by these tests that the oil and insulation are not in first-class condition because of the high temperatures to which they were subjected. The case is a good deal like that of automobile tires. There are tires in operation in which the tread has worn smooth and even tires in which the fabric is showing. These tires operate successfully, but, if reliability and safety of service is a factor, they cannot be considered to be in satisfactory operating condition.

These tests show that even with insulation in poor condition the transformers may still operate successfully. Since the oil deterioration was considerable for these tests and the insulation next to the copper must have been at a considerably higher temperature than the oil temperature, there is little doubt that the insulation had been damaged.

The attempt to set up certain definite safe temperatures for transformers is fundamentally difficult because there is no such thing as a safe temperature unless the time element and the permissible damage is also considered. Reliability of service is generally a first consideration, so much so that it may be desirable to work the transformer up to the absolute limit of failure before allowing service interruption. Any protective device which is put in for the purpose not of insuring continuity of service but for the purpose of protecting the transformer must reduce the total load which can be carried by a transformer without service interruption, otherwise no protection for the transformer is obtained. If the protection is set so high as to allow considerable damage to the transformer insulation, it may be well to omit the protection and allow more damage to the transformer insulation. The particular amount of damage which can be allowed before the transformer is tripped off is different for different applications but, in general, the best continuity of service i[s] obtained by protecting the transformer onl[y] against real short circuits and not for over[-] loads caused by useful load on the system.

It has been found that thermal device[s] which protect the transformer for the gen[-] erally considered safe operating tempera[-] tures provide undesirable service interrup[-] tions. The next natural step is to eithe[r] omit the thermal device or to operate th[e] thermal device with a less margin between its point of operation and the actual point of failure of the transformer and so that th[e] point of operation of the thermal device exceeds generally accepted safe tempera[-] tures for the transformer. If it is necessary to raise the level of protection to such a point that considerable damage is done to the transformer before the device oper[-] ates, it is questionable whether the device provides useful protection to the trans[-] former. Proof that the transformer wil[l] still operate does not constitute proof that the transformer is in a safe operating condi[-] tion. Transformers should be so operated that the insulation remains in good condi[-] tion unless an emergency condition arise[s] where it is necessary to damage the trans[-] former in order to secure continuity of serv[-] ice. In order that transformer insulation remain in first-class condition, the norma[l] loading of transformers should be such tha[t] serious damage to the insulation does no[t] occur. Overloads which damage the insula[-] tion should be on a strictly emergency basis. For service continuity it seems self-eviden[t] that if all transformers on the system have their insulation maintained in good condi[-] tion the service continuity will be better than if some of the transformers on the sys[-] tem are so operated as to have damaged insulation even if they do continue to operate. A device which offers protection only after considerable damage has been done may be desirable but it should be recog[-] nized that such a device offers very little protection against damage to the trans[-] former and may leave the transformer in an unsafe operating condition.

R. B. George (Tennessee Valley Authority[,] Norris, Tenn.): During some tests which I made to determine the short-time tempera[-] ture limits of transformer insulation, [I] recognized that there was a good margin which could be used for short time over[-] loads if the proper precautions were taken to protect the oil from oxidation and pro[-] visions were made to remove the load from the transformer when the magnitude o[r] duration of overload reached limits which would injure the transformer.

The paper by Mr. Putman and Mr. Dann describes tests that were made to deter[-] mine these limits. A device has been pro[-] posed to disconnect the transformer from the circuit when these limits are reached.

It is suggested that the proper com[-] mittees of the AIEE review the subject o[f] maximum temperature limits of insulating materials. It is always desirable to bring the AIEE rules up to date to include new developments in the art.

The results reported in this paper have an application for electric heating loads[,] particularly in the southern states. There are parts of this region where electric heat[-] ing would be required for a short portion o[f] the year.

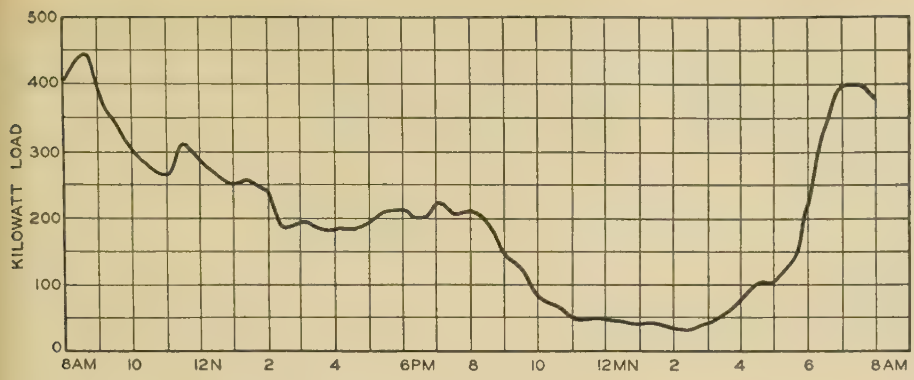


Figure 1

This load may be more economically served by using transformers with nominal kilovolt-ampere rating much lower than the peak load during the heating demand, and operating by copper temperature because electric heating is required only when the ambient temperature is low. The peak demand for electric heating usually occurs early in the morning and tapers off before the electric range load for noon cooking builds up its small peak.

Figure 1 of this discussion is a 24-hour curve of the total load on a circuit which serves 40 houses which are equipped with electric refrigerators, ranges, water heaters, and the usual appliances in addition to being electrically heated. The 17-hour average temperature from 6 a.m. to 10 p.m. was 19.9 degrees Fahrenheit. The 24-hour average temperature was 18 degrees Fahrenheit. This curve has the load characteristics which were described above. Since this curve also gives information concerning the duty on transformers for this type of load, it may be useful to a committee for reviewing the subject of maximum temperature limits of transformer insulation. Loads of this type can certainly be served more economically by taking advantage of operating by copper temperature during the peak loads which occur only at low ambient temperature.

A. C. Monteith (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The two outstanding factors brought out in this paper are the greater overload capacity of a transformer when the oil is kept free from contact with air and the fact that a reliable device is available to allow taking full advantage of this overload capacity without fear of burnout. Where regulation is not the determining factor in the transformer application, this overload capacity should allow fitting the transformer more closely to the load, thus allowing a reduction in initial investment. Since the "*CSP*" power transformer is equipped with step-type regulators, heating becomes the main factor in applying the transformer.

A secondary network system is designed so that the transformers will be called on to carry full load with one feeder out of service and carry some overload with two feeders out of service. Regulation is usually not a problem in this type of system, so that taking full advantage of a transformer having a tight tank and increased overload capacity should allow reducing the ratio of installed transformer capacity to full load. This is particularly true where networks are installed for supplying the lighter-load areas, as a greater chance can be taken as compared to the application for the heavy-load areas. Quite often the overload capacity given in the paper would allow carrying certain types of loads through the peak, which would mean applying the transformers based on their overload capacity rather than the continuous rating. Such factors should allow extending the economical use of the secondary network system.

The use of the thermal trip device applied to network transformers also presents a method of protecting against burnout in case of prolonged overload. Considerable time has been spent in trying to design a fuse that will leave the transformer on the system up to the danger point. The thermal device described in the paper will give this protection, the closing of its contacts tripping the network protector. If this type of protection is adopted, consideration could be given to eliminating the fuses from the network protectors.

These are a few of the changes in application that have occurred to the writer, if advantage is taken of the data presented in the paper. The new thought presented should go far to revolutionize our approach to the distribution problem.

T. H. Mawson (The Commonwealth and Southern Corporation, Birmingham, Ala.): The paper by Messrs. Putman and Dann is undoubtedly a valuable contribution to transformer operation. Previous studies by Nichols, Montsinger, Dann, and others have substantiated the operator's belief that transformers could be operated successfully without major reduction of life at ratings in excess of that shown on the name plate. In other words, the nameplate rating was only one point on the operating curve of the particular transformer.

It is, indeed, gratifying to know that the American Standards Association has prepared a "Guide for Loading Oil Immersed Distribution and Power Transformers," following the work of these men.

Since there are no satisfactory figures available on the life of transformer insulation due to the mass of test data, as yet unavailable, required for any sort of comprehensive averages, it is difficult to determine an actual life span for any transformer. The various methods mentioned for increasing the life of the transformer insulation such as inert gas in contact with the oil, reduced area of air contacting the oil, etc., play a part, but the oil temperatures, if allowed to remain at high levels for prolonged intervals must ultimately impair the condition of the transformer.

Loading to the limits set up in the ASA standards appears conservative in view of the test data in the present paper. While the test conditions for the five-kva transformers were unusually severe, how can the conclusions drawn from the tests be interpreted in terms of actual service conditions?

If transformers are to be installed on a basis of continuous and satisfactory service, the maximum use can be obtained from a given bank when the load curve is such as to permit overloading during peak hours to the point where the maximum temperature will not cause excessive deterioration. At the same time the possibility of excessive regulation in the transformer must be considered. Since this peak load would be considered as recurrent and not an infrequent emergency, the regulation in the transformer that would occur at these peaks would tend to be excessive. If this type of loading is to be considered for future installations then the design of the system components will have to be adjusted to operate within satisfactory voltage levels.

The use of some device to determine with reasonable accuracy the "hot-spot" temperature would give data that could be used in conjunction with typical load curves for various areas. This would provide a better method of determining the best size of transformer at that location, as well as the behavior of a particular transformer.

There is again the question of transformers now in service. Lack of knowledge as to the past history of these transformers would tend to raise questions as to the remaining life. Yet every effort should be made to work these transformers to their service limit—be it kilovolt-amperes or voltage drop.

The use of a lockout device for protection would be satisfactory, provided there was some control of the load, or that there was no great time lag between operation and reclosure. Therefore, operation of transformers at levels close to the limit would tend to increase the number of interruptions from this cause.

It would seem advisable to select transformers for both power and distribution service on a basis of the guide as set up by ASA, adjusting this selection where load data warrants, rather than depend entirely upon inspection of signal-light operation.

W. R. Brownlee (The Tennessee Electric Power Company, Chattanooga): Methods of securing maximum use of capital equipment as a means of reducing the over-all cost of electric power are of most vital interest to operating companies, to manufacturers, and to the entire industry. Contributions to such methods applied to distribution transformers are of particular value at this time.

For transformers of small size and value the bimetallic relay should find a most wide application, since it is a reasonably good device and can be secured at relatively low cost. They are much superior to fuses both in available characteristics and in reasonable adherence to predetermined

curves. I wonder if the authors are advocating the use of such devices for larger and more expensive transformers whereas, many operating companies have for years made use of more reliable (and more expensive) devices such as thermocouples or calibrated resistance units for operating power transformers by copper temperature. Sometimes these devices are permitted to trip breakers but particularly in case of the larger units they provide a graphic record of temperature, permitting the system operator to exercise his judgment in balancing the probable shortening of life of the transformer unit due to the overheating, against the seriousness of the effect on service or on other equipment involved in taking the unit out of service.

Apparently the authors have relied on dielectric tests to determine whether the transformers under test suffered any damage due to overloads. Have they actually taken any of the tested units apart to determine the exact condition of the inside insulation? If such critical examination should confirm their preliminary findings, then it might be in order for the proper AIEE committees to review the subject of maximum safe temperature limits.

Probably the authors intended to include power transformers with conservators along with transformers equipped with "inertaire" equipment in contrasting the effects of over-temperature of materials tested in open oil and deterioration in oil protected from open air. Naturally gas-tight cases are preferred for small distribution transformers or for any underground installation.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): The authors have added a real contribution to the art of loading transformers by temperature under various service conditions.

There are, however, some statements made in the paper to which I must take exception. The paper states that the signal light is set to operate at about 95 degrees centigrade (permitting continuous operation at 95 degrees average copper temperature) which limit they say is recognized in the AIEE Standards. This is correct, but it has generally been agreed by the industry for the past three or four years that for continuous operation the average temperature should not exceed 85 degrees or 55 degrees rise in a 30-degree ambient. They may intend that the 95-degree average temperature limit be used only for short-time operation. If so, what is there to prevent the transformers operating continuously at average copper temperatures close to 95 degrees centigrade? This point is not clear.

As a matter of fact, AIEE Standards No. 13 will be superseded within a few months by the Proposed American Standards and Recommended Practices for Transformers, which recommend that for continuous operation the average copper temperature not exceed 85 degrees centigrade.

If the conditions are such as to permit continuous operation at 95 degrees average temperature by the signal light, I would like to suggest that the authors either recommend the temperature limit of 85 degrees centigrade as given in the new guides for operation of transformers, which are a part of the proposed American standards mentioned above, or give a valid reason for (or data to support) setting the temperature limit ten degrees higher than recommended for continuous operation in the proposed guides.

The authors make the statement that when the oil is protected against the harmful effects of oxygen by "inertaire," there is practically no deterioration of the insulating materials at the limits of temperature suggested by the Institute and the American Standards Association. I think the authors should state whether they mean the Institute or ASA since, as stated above, the temperature limit recommended by ASA is ten degrees lower than that recommended by the Institute.

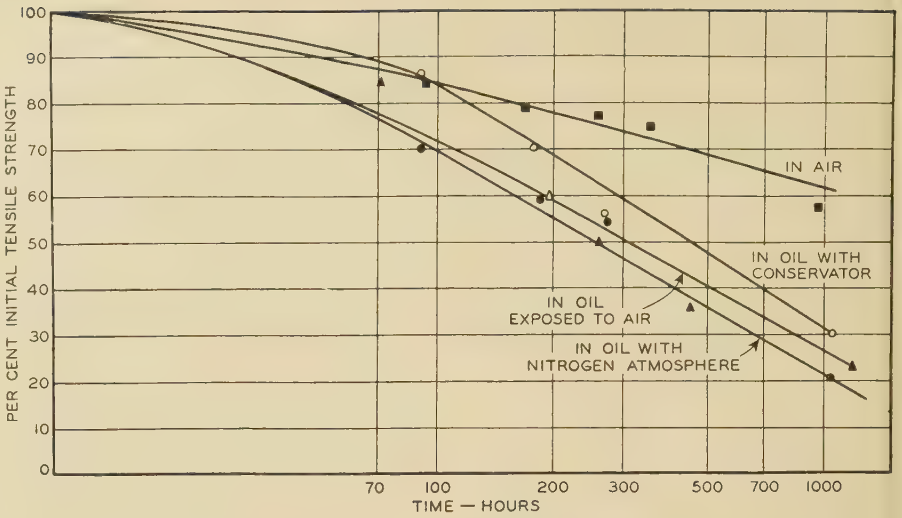


Figure 3. Aging of class A insulations at 105 degrees centigrade

Materials used: (1) 0.031-inch pressboard; (2) 0.010-inch kraft paper

Tests made in 1935

The authors state that all aging tests of class *A* insulations in the past have been made in oil exposed to air and by inference say that most, if not practically all, of the aging of insulation in oil at 95 degrees average temperature (with the hottest spot ranging from say 98 to 105 degrees depending on the design) is due to the presence of oxygen in the oil. This does not agree with what I have found.

In 1935 our laboratory made two separate series of aging tests to determine the difference in the aging (tensile strength) of various class *A* materials immersed (1) in air, (2) in oil exposed to the air, (3) in oil with a nitrogen atmosphere, and (4) the oil protected by a conservator.

Both untreated and treated materials were tested. The treated samples were given a 30-minute varnish dip, centrifuged to remove excess varnish, and baked to set up the varnish. The materials were cut into pieces approximately ten inches square and then assembled in packs, part of the materials being spaced with pressboard spacing strips to allow circulation of the oil or air, and the remaining materials assembled in a tightly-packed mass.

The test conditions were made to represent as nearly as possible the conditions of transformers in service.

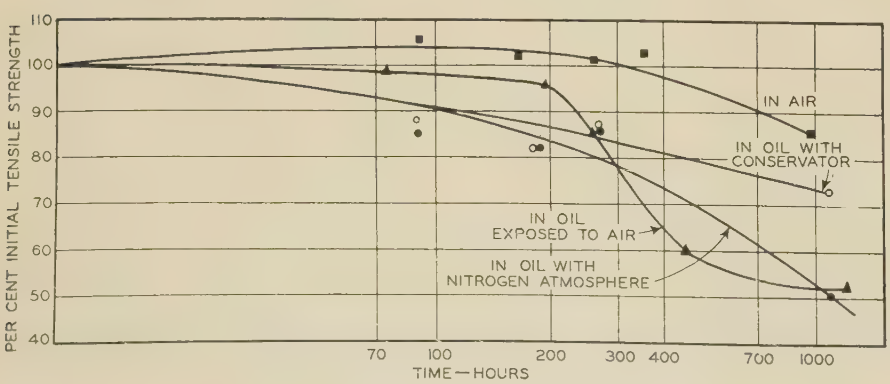
The materials which were aged in oil were immersed in oil in steel containers and placed in an oven maintained at a constant temperature of 105 degrees centigrade plus or minus one degree.

In the case of aging in oil with conservator, the connecting pipe flush with the cover was connected to the conservator on the outside of the oven.

The tank with the nitrogen atmosphere was connected to a bomb of oxygen-free nitrogen through a pipe leading to the bottom of the tank. Another pipe flush with the cover was connected to a pressure release valve set to a pressure of one-half pound. After the insulation and oil were placed in the tank, the oil being at a level

Figure 2. Aging of 0.031-inch pressboard at 105 degrees centigrade

Tests made in 1935



of about two inches below the cover, nitrogen was flushed through the oil for a period of one hour, then the nitrogen was shut off, and the aging started. Each time the tank was opened to remove samples, the air was thoroughly flushed from the oil with nitrogen before resuming the test.

The container in which the material was aged under oil with air over the oil was an open tank.

The aging in air was accomplished by placing bundles of the insulation in an oven maintained at a constant temperature of 105 degrees plus or minus 1 degree centigrade, taking care to keep the material from contact with metal in the oven.

All samples were conditioned in a standard atmosphere four hours before testing. A set of standard samples was immersed in the aging medium one hour at room temperature, conditioned four hours under standard humidity conditions, and tested to obtain standard data for the aging comparison.

The results of the aging tests are shown in figures 2 and 3 of this discussion which indicate:

1. That insulation aged in air has a longer life than insulation aged in oil.

2. That insulation aged in oil plus air, or in oil plus conservator, or in oil plus nitrogen atmosphere, shows approximately the same amount of mechanical deterioration.

There was practically no difference between the rate of aging of the materials spaced or unspaced in oil. In air, the spaced materials showed approximately ten per cent less aging.

The authors state that to determine whether the heavy overloads had injured the transformer, AIEE dielectric tests were applied. I do not feel that these tests mean anything, since insulation can be greatly weakened mechanically before its dielectric strength is affected. In fact, the dielectric strength of oil-immersed insulation does not decrease until it is well carbonized and cracked. I note that a few short-circuit tests were made, which were probably a better test on the insulations than the dielectric tests.

I would like to suggest that a better method of determining the effect of short-time overloads on the insulation would be to integrate the heating curve area in some such manner as I used in my AIEE paper entitled "Temperature Limits for Short-Time Overloads for Oil-Immersed Neutral-Grounding Reactors and Transformers" published in AIEE TRANSACTIONS, volume 57, 1938, pages 39–44 (January section). This method of analysis ought to give some idea of whether 500 or 600 load cycles has appreciably weakened mechanically the insulation.

H. V. Putman and W. M. Dann: The discussions on the paper "Loading Transformers by Copper Temperature" have brought out a lively interest in the subject. The purpose of the paper was to give emphasis to the recognized fact that transformers inherently have a substantial overload capacity for short-time periods and to describe a practical way of taking advantage of this capacity automatically in service. The prevailing views of those who discussed the paper seem to be that the utilization of this short-time overload capacity is of vital importance to the operating companies and to the industry in general and that in the main the method of obtaining its full utilization is effective and practical.

Messrs. Christensen, Hamilton, George, and Brownlee suggest either that the AIEE temperature limit of 105 degrees centigrade may be too low or that perhaps the Institute should review the subject of limiting temperatures. As pointed out in the paper, we believe these suggestions should be carried out, particularly in connection with limiting temperatures for short time periods.

Mr. Mawson speaks of the "Guide for Operation of Transformers," which is about to be published by the ASA, and comments that it would seem advisable to select transformers on the basis of this guide rather than to rely entirely upon signal-light operation. However, the overloads suggested by the ASA guide are quite conservative; they had to be because they apply to transformers of more than one type and to units that have been in service for the past ten years. Modern transformers operated by copper temperature with the relay and signal device will, of course, carry the short-time overloads of the ASA guide, but they will go further; the signal light will give a warning when a definitely established temperature is reached and will cut the transformer out if the operation is continued until a temperature is reached at which appreciable shortening of life would result. Furthermore, ambient temperature is automatically taken into account and it is unnecessary to consult tables of permissible overloads.

Mr. Montsinger points out that the signal light is set to operate at about 95 degree centigrade and he asks whether there is anything to prevent operating a transformer continuously at an average copper temperature close to 95 degrees centigrade. It is a fact that the transformer, just like an ordinary unit, could be artificially loaded so that its temperature rise plus the ambient temperature would be continuously just inside the limit of signal operation and its average winding temperature would be continuously close to 95 degrees centigrade. But it is hardly conceivable that a transformer would be called upon to carry its full load continuously in actual service with an ambient temperature which is continuously 40 degrees centigrade. Load conditions and ambient temperatures in actual service are largely uncontrollable; they normally vary throughout the day and the season, and in cases where a transformer is so small with regard to its load conditions that its average copper temperature occasionally reaches 95 degrees centigrade or thereabouts, it seems obvious that it would be better to know of the conditions through signal-light operation than to be unaware of them. A very natural question arising from Mr. Montsinger's inquiry is—what is there to prevent *any* distribution transformer from operating continuously at 95 degrees centigrade?

Mr. Mawson asks how the conclusions drawn from the tests on the five-kva transformers may be interpreted in terms of actual service conditions. It is not possible in a laboratory to simulate a large number of the overload conditions that can exist in actual service, but the tests do represent very severe cases of certain types of overload, and operation at other overloads in service may be sized up by analysis and comparison.

Mr. Mawson speaks of the life span of transformer insulation, having in mind the effect of temperature, while Mr. Starr reminds us that repeated overloads reduce transformer life and that there is a threshold of loading beyond which overloads are uneconomical. Mr. Sealey introduces a striking comparison to automobile tires and says that the oil and insulation in the transformers under test are not in first-class condition because of the high temperatures to which they have been subjected. It is of course too early to say what the condition of the insulation actually is, but there is nothing in the tests themselves to indicate that the oil and insulation have been seriously damaged. At the time the paper was written, after 660 cycles of severe overloading, the transformers had more than once withstood the standard AIEE low-frequency and impulse tests and the oil, while darkened in color, had withstood even higher breakdown tests than before the cycle tests were started. These dielectric tests and the fact that the transformers are still going through similar cycles of tests indicate that they are still good for actual service.

Mr. Montsinger questions whether the successful application of Institute tests means anything, and he points out that insulation can be greatly weakened mechanically before its dielectric strength is affected. That was the very reason for subjecting the transformers to a series of short-circuit tests with an ignitron timer. The violent vibrations of those tests failed to develop any mechanical weakness of the insulation, for the transformers immediately afterward withstood impulse tests and tests at double-voltage excitation. When the cycle tests are finally discontinued the units will be dismantled and the insulation minutely examined. The results of this examination will no doubt form a contribution to the study of the life of transformer insulation.

Mr. Brownlee asks whether the relay and signal devices are advocated for large transformers, and Messrs. Christensen and Hamilton raise the question of adjustment, maintenance, and replacement of the devices in large power transformers. These devices are right now being used in service with complete satisfaction on transformers as large as 1,500 kva. They can be used effectively for power transformers of any size. A convenient adjustment of the relay is provided for, but field experience with more than 200,000 "transformer-years" in service indicates that adjustment has not been necessary and that maintenance attention will be rarely needed. If a relay were to fail it would have to be replaced.

Mr. Monteith comments on the considerable time that has been spent in trying to design a fuse that will leave the transformer on the system up to the danger point, and he points out the value of the relay for this kind of protection, while Mr. Brownlee remarks that the relay is much superior to the fuse.

Mr. George speaks of the early morning peaks caused by electric heating loads. This is an excellent illustration of a peak that can be taken care of with transformers having ratings much lower than the peak

Effects of Temperature on Mechanical Performance of Rotating Electrical Machinery

C. LYNN
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THE EFFECTS of temperature on electrical machinery are usually considered in connection with the deterioration of insulation used on the active conductors themselves. There are, however, many other parts of these machines where temperature effects are important and vitally affect the design of the machine. Unless provision is made in the design of machines to take care of these effects, unsatisfactory operation or reduced life may result. These effects on machine construction may be considered in three groups.

1. Effects of Temperature on Insulating Parts

Machines with class *A* insulation on the windings have class *A* materials on other parts of the machine as well as on the windings proper. On d-c machines, fullerboard insulation is used around the main and commutating poles to give ground insulation and creepage to ground. On a-c salient-pole machines, fullerboard or fish paper is commonly used for a similar purpose on the rotor poles. Where higher temperature rises than those permitted for class *A* materials are encountered, these materials must be abandoned in favor of class *B* materials such as asbestos, mica, or woven-glass formed shields. To eliminate formed channels and insulating pieces around the stationary pole pieces, railway motors and other propulsion equipment requiring maximum output with minimum space and weight have the necessary insulation protection to ground incorporated in the coil insulation itself. Mummified coils using class *B* conductor insulation are provided with sufficient mica or asbestos insulation on the outside of the coil itself to give normal protection to ground.

If temperature limits are pushed very high, such machines as enclosed self-ventilated motors would get extremely high temperatures on all internal parts and fibrous materials, such as used for insulation between brush holder brackets and rocker rings, would no longer be satisfactory, and special porcelains or molded mica materials as used on railway motors for brush-holder-stud insulation would have to be used.

On both a-c and d-c machines, fish-paper cells of class *A* material are used in the slots, not from an insulation standpoint but to provide, during winding, protection to the coil sides against the edges of the laminations in the slots. With higher temperatures such cells are abandoned or mica cells can be substituted.

Wedges for class *A* machines are universally made of fiber. Excessive temperatures cause such materials to soften, shrink, and sometimes split or crack. When wedges become loose or crack, they generally work endwise out of the slots, although sometimes they push radially out of the slots, especially near the ends of the core. Substitutes of Micarta or other phenolic base materials may be used. High temperatures are also detrimental to the fiber winding strips that are used under the wedges to provide a sliding base for the wedge when being driven in the slot grooves and to provide a means of securing a tight coil in the slot. Mica-base material must then be substituted.

All d-c machines, wound-rotor induction motors, and rotating-armature a-c machines use bands of wire to hold the end windings of the rotors in position. On almost all except the smallest machines, coil supports are used under the end windings and the coils are held down against these coil supports on the end portion. Channel or other types of insulating material are used on the coil supports to provide insulation and creepage to ground. Layers of insulating material and fullerboard are placed around the armature coils on the ends to provide insulation and protection to the coils from the steel band wire which holds the end windings in place. High operating temperatures cause this insulating material, as well as the insulation on the coil supports, to shrink and thus the coils and bands will become loose on the end windings, unless special materials and precau-

Paper number 39-17, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 22, 1938; made available for preprinting December 8, 1938.

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load, when loaded by copper temperature with the relay and the signal device.

The results of Mr. Montsinger's tests extended over a period of only about 40 days, which is hardly sufficient to give reliable conclusions. To state it simply, Mr. Montsinger's conclusions are that cellulose insulating materials immersed in new transformer oil at 105 degrees and fully protected against oxidation and moisture deteriorate just as rapidly as when the oil is subjected to oxidation and moisture by exposure to the atmosphere. This conclusion does not agree with the findings of our own research engineers and the results obtained by others, notably Stager (*Elektrotechnische Isoliermaterialien*, Stuttgart, 1931).

In a paper "Temperature Limits Set by Oil and Cellulose Insulation" by Doctor C. F. Hill (AIEE TRANSACTIONS, volume 58, 1939, pages 484–91) it is shown by carefully conducted tests extending over a period of approximately 650 days, that the deterioration of cellulose materials eventually flattens out and ceases, but that it takes more than 40 days to reach this condition. His tests show that electrically such materials maintain their initial characteristics even when above 100 degrees centigrade and they may even show improvement; this is true up to 140 degrees centigrade when the materials and oil are protected with an inert gas. Mechanically, untreated cellulose in air deteriorates rapidly above 100 degrees centigrade, or at 95 degrees centigrade when the oil is exposed to air. However, his opinion is that many transformers are operating satisfactorily in service in which the insulating materials have deteriorated to one-fourth of their original mechanical strength.

The conclusions drawn from Doctor Hill's tests are that the deterioration of insulating materials is greatest when the oil is exposed to air, and least when it is protected by an inert gas. Mr. Montsinger's tests show that the deterioration of pressboard mechanically at the end of about 40 days at 105 degrees centigrade is about the same as in oil exposed to air and in oil protected by nitrogen. This is not thought to be the case and it is not substantiated by Doctor Hill's tests. Mr. Montsinger's tests also show considerably less deterioration in oil with a conservator than in oil protected with nitrogen. It is probable that in the conservator tests there was no breathing of air and the results obtained were really due to a tightly sealed tank, which is the case with the transformers discussed in the paper on "Loading Transformers by Copper Temperature."

tions are taken to eliminate this shrinkage of the insulating materials.

Successful operation of commutators depends more on their ability to go through a temperature cycle than upon any one other feature. By this is meant their ability to keep a smooth surface, and not have bar to bar roughness with an increase in temperature. Eccentricity of commutators up to 0.001 of an inch even on high-speed machines will not affect the operation, but bar-to-bar roughness or unevenness of greater than 0.0001 inch will usually cause sparking and commutation trouble. Obviously for a given commutator, the less the temperature range the less chance there will be for bar-to-bar roughness. This does not mean that commutators should not operate at fairly high temperatures, nor does it mean that commutators should be specified to operate at excessively low temperatures.

The temperature resulting on a commutator is dependent upon the losses occurring at the commutator, the brush contact I^2R loss, and the brush friction. The I^2R loss of the current flowing in the commutator bars to the brushes is insignificant and never measured nor considered due to the large cross-section area of the commutator bars themselves and because the current flows in the bars only during the short time they pass under the brushes, during the commutation period. The loss at the brush contact due to the voltage drop of the current in passing between the commutator and brushes does vary somewhat with the material of the brush and is dependent upon the surface condition of the commutator. However, for all practical purposes, the voltage drop can be considered constant at one volt per contact. This loss, therefore, varies directly with the load current. The brush friction loss depends upon the number and size of brushes, the material of the brush and the condition of the contact surfaces, the brush pressure and the peripheral speed. The brush area for a given current rating is limited and cannot be reduced below an accepted value so that the total brush loss for a given set of conditions cannot be reduced except by decreasing the commutator diameter. Roughly, as the diameter is reduced the brush width must also be reduced, so that to maintain the required brush area more brushes must be added. This requires a longer commutator with resulting larger spans between supporting rings or greater overhang, both of which result in greater stresses and deflections with an increase in bar-to-bar roughness for a given temperature rise. Obviously then, it is not desirable to specify too low an operating temperature for the commutator as this will require a greater heat dissipating area, with resulting greater stresses, deflections, and roughness, with accompanying poorer commutation.

What then limits the temperature of a commutator in an upward direction? Not limitations of temperature of the mica between commutator bars and between the bars and the ground parts, that is, the vee-ring or shrink-ring mica, as this mica will withstand temperatures considerably higher than permissible or acceptable for successful commutator operation. Extremely high-temperature operation of the commutator could result in damage to the insulation on the armature coils due to heat flow from the commutator up through the necks and into the armature coil. However, the thing that limits commutator temperature is the ability of the commutator to go through a temperature cycle without bar-to-bar roughness and this depends primarily on the characteristics of the insulating mica used in the commutator and the processes used in building the commutator.

At the present time, full-load continuous-operation acceptable temperatures for commutators of class A insulated machines are 105 degrees centigrade and of class B insulated machines 125 degrees centigrade, with temperatures measured by thermometers, and with no allowance for hot spots, as obviously the maximum temperatures can be measured directly. Based upon present experience and designs, higher temperatures than 125 degrees centigrade for continuous operation are permissible, probably in the neighborhood of 150 degrees centigrade, still permitting successful operating commutators.

2. Influence of Heat on Materials Other Than Insulation

There is another limitation on the operating temperature of commutators due to the nature of the material used. Commutator copper itself must not be heated to too high a temperature or it will become annealed and thus lose its mechanical strength. The annealing temperature is not definite for copper but is influenced by time. For instance, commutators can operate indefinitely at a temperature of 150 degrees centigrade without any chance of annealing the copper. If the temperature is raised to 175 degrees centigrade, copper will not anneal immediately as it takes hours and days to anneal the copper thoroughly. If the temperature is increased appreciably above 200 degrees centigrade, the copper can be damaged in a short space of time.

Another limit in commutator temperature operation is the solder used in attaching the armature coils to the commutator necks. Ordinary solder melts at 180 degrees centigrade. Hard or tin solder melts at 220 degrees centigrade. These solders if used in commutator bar construction can be used without permanent damage to the commutator copper when soldering. However, if the commutator is subjected, for short durations of time, to very high temperatures, the solder may melt and be thrown out due to centrifugal force. This then forms high resistance joints or open circuits in the armature winding and sparking results.

Commutators for high-temperature-operating machines, such as railway motors, are usually made of silver-bearing lake copper as this material has better temperature creep characteristics and will better stand the stresses due to higher-temperature operation. The use of higher-temperature solders, melting at temperatures between 250 and 300 degrees centigrade, will eliminate the problems of the joints between the commutator necks and armature coils opening up due to high temperatures of operation and stresses of rotation. Its use, however, introduces problems of soldering without annealing the copper bars during the soldering operation, especially on those commutators having solid necks. Brazed or phosphorous solder joints which will stand relatively high temperatures could be used, but such joints do not permit satisfactory opening for removal of armature coils in case of repairs or replacements and there is the possibility of damage to the coil insulation in its use.

Brushes of carbon, carbon graphite, and copper graphite types are baked at temperatures far above those experienced in service. However, these brushes have shunts of stranded flexible cable attached to them by riveting or soldering and operation at too high a temperature will result in selective action and unequal current distribution, with resulting overloading of some brushes. This will cause overheating of the brush shunts, giving discoloration and brittleness to these shunts as well as the melting of the solder used in the shunt attachments, causing still further detrimental operation. Too high an increase in temperature operation of current collecting parts always results in poorer operation, more rapid brush wear, and higher maintenance costs.

Higher temperatures will adversely affect the insulating treatments used on

the punchings. In some cases varnish treatments will have to be supplanted by other forms of insulation. It can also detrimentally affect varnish treatments applied to various types of insulating pieces.

Oil-lubricated sleeve bearings may be affected by machine temperature and in extreme cases with high operating temperatures, external cooling of the bearings must be used. In the past oil cooling of the bearings by circulating water through pipes embedded in the bearing shell was used. Today the tendency is to use external coolers and circulate the oil through these coolers.

3. Problems of Expansion and Contraction Due to Temperatures

Method of bearing support as well as bearings themselves are affected by the temperature of operation. Antifriction bearings must not bind, neither must they have too loose a fit or they will be noisy and eventually give trouble in operation due to this looseness. However, if the machine operates at relatively high temperatures, the antifriction bearings must be of the loose-fit type having greater than normal clearance between the balls and races when at room temperature so that while there may be some extra looseness when the bearing is cold, normal tolerances will be secured at operating temperatures. Antifriction-bearing machines must have at least one bearing free to move endwise with temperature expansion. This means that where provision must be made for end thrust, end thrust in both directions must be taken by the bearings on one end of the machine only.

Most difficulties on induction motors due to temperatures are in connection with squirrel-cage windings. On those motors having wound rotors the problems are similar to those of d-c machines, commutators of course excepted.

On the former types of windings, the bars in the rotor slots are usually of copper and project beyond the ends of the punchings, where they are attached to the end rings. The end rings are made of copper, brass, or bronze. In the usual construction, the bars are brazed to the end rings. The difficulties encountered are due to the temperature expansion of the end rings. As they get hot, they expand in diameter and in so doing, bend the ends of the bars outward. On cooling, the reverse bending occurs. Repeated cycles of heating and cooling, due to repeated starting and stopping and changes of load, can cause these bars to break. The breaks occur in the bars between the laminations and the end rings. A break of a brazed joint is a rarity.

It can be shown that the loss produced in the squirrel-cage winding during starting is equal to the kinetic energy stored in the rotor and its connected load at rated speed. Since the starting periods are of short duration, 10 to 15 seconds on large machines, most of this loss is absorbed in the squirrel-cage winding, since this short time does not permit dissipation of the loss in the surrounding air. On slow-speed motors the WR^2 of the rotor itself is quite high and the WR^2 of the load may be several times that of the rotor, in many applications such as induced draft fans, grinders, choppers, and saws. In all these instances the large amount of loss raises the squirrel-cage-winding temperature to a high value with corresponding untoward results. By using brass or bronze end rings of higher resistance, larger cross-section rings can be used with no change in total resistance but with increased heat absorbing capacity. This results in less temperature rise, less expansion, and less breakage of bars.

On the smaller sizes of induction motors, where very small air gaps are used, very high temperatures will result in the rotor scraping on the stator at times. The rotor will run hotter than the stator so that the expansion will be greater, especially on high-slip motors which have many applications to take advantage of the flywheel effect that the high slip permits. Since production dictates noncircular outside-punching peripheries to get the maximum number of punchings from a given sheet of steel, resulting in only a portion of the punching periphery being held in the stator frame, the expansion of the stator punchings will not be symmetrical. This results in unequal expansion of the inside diameter of the stator punchings so that with the expansion of the rotor, the air gap will be so small at some point around the periphery of the motor that the rotor will rub on the stator. Even where the expansion does not actually cause the rotor to rub the stator, the air gap at some one point will be so small that the unbalanced magnetic pull will deflect the shaft enough to cause actual rubbing of the rotor on the stator punchings. Higher temperature rises will also require larger press fits of the punchings on the shaft so that the increased unequal heating of the punchings and the shaft will not cause the punchings to become loose on the shaft.

If higher-temperature-rise motors were used, especially in the larger sizes, the motor dimensions would be decreased, resulting in still higher temperature rises of the squirrel-cage winding with again unfavorable characteristics.

Collectors as used on the a-c end of synchronous converters, wound-rotor induction motors, and rotating-armature a-c machines are also subjected to temperature limits. These limits, since there are no bar-to-bar roughness conditions to be met, can be somewhat higher than for commutators. A safe ultimate temperature of 150 degrees centigrade could be used for collector-ring operation. The connections to collector rings are usually made by means of rods threaded into the collector material below the body of the ring surface. These threaded rings are usually sweated into position and too high a temperature operation of the collector rings, even for short periods of time, will result in the melting of this solder. High temperatures on collector rings have a tendency for the rings to become out of round, particularly on the large sizes, resulting in a roughened surface and accompanying poor operation and faster brush wear. On the larger size collector rings used on synchronous converters the spokes or arms can be made S shaped instead of radial so that the stresses due to expansion will not tend to distort the ring from a true circle. The ring body can also be made of relatively heavy cross section, even T shaped, in order to hold the ring surface as concentric as possible with an increase in temperature.

On relatively long-core d-c machines the ends of the field coils are not supported other than by the material itself and being free to move due to expansion, no chafing of the insulation results.

In the largest sizes of turbogenerators, with lengths of 20 to 25 feet between bearings, temperature rises up to 85 degrees will give elongations of approximately $^1/_8$ inch to $^5/_{16}$ inch in the various parts. The steel parts of these long-core machines operate at lower temperatures than the copper conductors, and since copper has a greater temperature coefficient of expansion than steel, there can be a total difference of endwise expansion of approximately $^3/_{16}$ inch of the copper conductors over that of the steel rotor, in which the copper conductors are embedded. Overload requires increased excitation and gives a greater increase in copper losses than increase in the iron loss, thus further accentuating this difference of expansion.

On these high-speed turbogenerators, the centrifugal force, due to the weight of the conductors in the rotors, exerts such enormous forces against the underside of the slot wedges that the conductors are

partially restrained from total movement in respect to the iron, throughout part of the total length due to those different temperatures and coefficient differences. Thus, most of the relative movement takes place at the ends of the rotors. This has in the past resulted in the insulation and bracing in the end rings of the rotors being chafed and can result in ultimate failure. However, improvements in insulation and the design of the end ring bracing in present designs give a construction that operates satisfactorily. A further increase in temperature rises would again extend the expansion beyond the limits of satisfactory operation.

On the stators, with vent ducts spaced in the iron for adequate ventilation, the insulated conductors will be exposed in the vent ducts. The insulation, being unrestrained by any slot sides in the vent ducts, will bulge with time, into these vent ducts. Since, as in the rotors, the copper conductors expand more than the core iron, a relative movement of the former will cause chafing of the coil insulation at the vent ducts. A large number of machines having a 60-degree-centigrade rise of insulated coils have operated for 15 years without trouble due to this elongation. An attempt, however, to increase this temperature rise from 60 degrees centigrade to 100 degrees centigrade on comparable size machines gave only a few years life due to failure of insulation at the vent ducts due to the endwise expansion differential. Failures of this nature are due to the pulverizing of the mica flakes due to the repeated cycles of chafing caused by heating and cooling with load changes.

Thus, it can be seen that excluding temperature limitations of the insulation on windings there are many features of machines, both electrical and mechanical, that place temperature limits on the operation of rotating electrical machinery. There are still other problems in connection with temperatures that vitally affect the design and operation of the machines.

From the performance standpoint, an increase in temperature rises, permitting more output from a given size and weight of material, reduces the margin, particularly of overload characteristics, even on short-time overload ratings. For instance, applying a higher-temperature-class insulation to a given design d-c machine with a redesign in the loading ratios of flux capacity to current capacity, in order to obtain a balanced design for the higher permissible temperature rises, will not permit increased overload commutating capacity in proportion to the increased capacity secured at the expense of increased temperature rise. Except for working the iron harder—that is, increasing the flux densities, giving a relatively lower armature-current loading—the overload commutating capacity is unchanged. In fact, the higher temperature operation of the commutator at the increased full-load rating, resulting in a somewhat higher bar-to-bar roughness, decreases slightly the commutating ability at any given short-time overload rating. Commutating overload ability is usually limited by the flux-carrying capacity of the commutating pole, which obviously is not increased by higher permissible temperature limits. Thus when higher ratings at higher temperatures are specified and secured, overload ratings must be decreased. However, on those applications where heavy overloads are not required or are of infrequent occurrence, and not of very great magnitude, higher-temperature machines of smaller size and weights can be produced.

Inherent voltage regulations, especially of d-c machines, drop off quite rapidly above normal full-load ratings. Increasing the ratings with accompanying greater temperature rises is secured at the expense of poorer voltage-regulation performance.

Many times extraordinarily good performance in the line of efficiencies is required from machines by the purchasers, while at the same time high temperature rises are permitted. Obviously if materials are worked very hard to get minimum material for maximum output, with resulting high temperature rise, efficiency must be sacrificed. Extremely high efficiencies can only be secured by not working all materials up to the maximum and some class *B* rated machines may have temperature rises falling in class *A* ratings simply because sufficient material had to be used to secure high efficiencies, resulting in minimum losses and relatively low temperature rises.

Machines may sometimes be purchased by customers specifying class *B* insulation, where the actual requirements could be met with class *A* insulation features. This is done intentionally so that for emergency operation the equipment can operate at higher temperatures without failure. In this connection, distinction should be made between the temperatures encountered in frequent short periods of cycles of operation and those encountered in continuous operation. Obviously high temperatures are considerably more detrimental for continuous operation.

Railway motors and other propulsion equipment require maximum output in minimum space and with minimum weight. This equipment, therefore, logically belongs in high-temperature classification and some sacrifice of operating life, increased maintenance, and limitations on some performance characteristics are permissible to get maximum output rating with minimum space and weight.

It is not the purpose of this paper to discourage the use of higher temperatures in rotating electrical machinery as in many cases such a step is very desirable, but it is the purpose to indicate the many factors that are vitally affected by such increase in temperatures. The use of higher temperatures will bring new problems along many lines, especially in large size machines. Caution in procedure, based upon experience gained, is advisable. Evolution rather than revolution of existing standards upward should be the trend.

Discussion

Felix Konn (General Electric Company, Erie, Pa.): I would like to emphasize Mr. Lynn's very appropriate statements about the higher operating temperature of traction motors and about the temperature of commutators.

If we assume that, by increasing the current and the speed, we obtain more output from a given piece of commutating machinery (by accepting higher temperature rises) this will result not only in higher commutator temperatures because of the increased losses but also in an increase in the commutating duty.

If we think of the commutating duty in terms of the reactance voltage, determined as the product of:

commutated current $\times$ rpm $\times$ K

(where K is determined by the design of the machine) we see that whether we increase the current or the speed or both, we increase the commutating duty. This will result in increased commutation loss, causing an additional temperature rise of the commutator and of the armature winding, but, more important still, if the commutating capacity of the machine is inadequate for the increased load, this higher commutating duty will endanger the performance of the machine by causing excessive sparking at the brushes resulting in burning of commutator segments and rapid brush wear.

It is, therefore, very important that, in the design and manufacture of light-weight commutating machinery operating at high temperature rises, considerable attention be given toward providing the most favorable commutating conditions (electrical and mechanical) in order to maintain the proper balance between commutating and heating capacity. This consideration has governed the design of d-c and a-c traction motors and, far from hindering the progress toward

higher outputs per pound, the fact that each machine has, so to speak, two ratings which should be matched one to the other has resulted in continued advances in both directions of heating and commutating capacity.

E. F. Dissmeyer (The Commonwealth and Southern Corporation, Jackson, Mich.): Mr. Lynn's discussion of "Effects of Temperature on Mechanical Performance of Rotating Electrical Machinery" presents a number of important items which should be given serious consideration when contemplating revision of our present standards. Mr. Lynn's paper covers in detail certain of the comments which the writer outlined in his discussion of Mr. Hellmund's paper "Rating of Electric Machinery and Apparatus" (AIEE TRANSACTIONS, volume 58, 1939, pages 499–503).

A number of failures of synchronous-condenser and frequency-changer rotors have occurred due to mechanical damage resulting from thermal effects. Expansion or movement of conductors has resulted in turn-to-turn and other types of rotor-insulation failures. Inspection of rotors has also disclosed many other troubles resulting from thermal effects, such as distortion or creep of amortisseur windings. The design of such rotors should not present any particularly difficult mechanical problems and consequently many of the troubles which we have with large machine rotors can be directly attributed to the fact that we permit rotors to operate at relatively high temperatures. There consequently appears to be considerable justification for reducing the permissible temperature rise of large machine rotors.

F. W. Gay (Public Service Electric and Gas Company, Newark, N. J.): The mechanical limitation imposed on a machine due to the fact that the lineal expansion of steel is only two-thirds of the lineal expansion of copper becomes most acute in very long 3,600-rpm turbine generators.

As an instance of this limitation it was at first expected that a 50,000-kw 3,600-rpm synchronous generator rated on the basis of 87-degrees Fahrenheit cooling water could carry 55,000 kw in the winter with a cooling water temperature of 50 degrees. With this idea in mind, a large refrigerating manufacturer was asked to submit a proposition to refrigerate the summer cooling water of 87 degrees Fahrenheit to 45 degrees Fahrenheit, well below the average winter temperature. The figure submitted to cover the cost of refrigerating machinery to give the expected 5,000 kw of incremental capacity with a power consumption for this machinery of approximately 125 hp was approximately $15,000. This looked like an attractive proposition, but the generator manufacturer quickly explained that even if the cooling water were cooled close to zero degrees centigrade no incremental kilowatts could be obtained. Apparently the limitation on this machine was the unequal mechanical expansion of its component parts rather than any hot-spot temperature.

It would appear that the time has come when copper conductors must be directly refrigerated and maintained at a temperature two-thirds that of the surrounding iron. When this can be achieved the expansion of the copper will equal that of the steel and the mechanical bugbear outlined in Mr. Lynn's paper can no longer frighten us. New refrigerating materials now available may soon make this practicable.

C. Lynn: Mr. Dissmeyer's and particularly Mr. Gay's experiences in higher-temperature operations of rotating electrical machinery stress the difficulties on insulated conductors due to expansions encountered in such operations. Obviously, these difficulties can be minimized and the limits extended somewhat by proper design. Too high-temperature operation will be at the expense of shorter life of the machine.

The point of balanced design between commutation and heating capacity in commutating machinery, as brought out by Mr. Konn, is well taken as the one of these which first reaches its limit determines the capacity of the unit. In this same connection it should be noted that a requirement of too low a temperature operation can lower the output capacity of a machine. For instance, for a given amount of loss on a commutator, the temperature rise can only be lowered by increasing the heat dissipating surface. If this is accomplished by increasing the length of the commutator bars, additional rotational stresses will be set up in them due to the greater spans between vee-ring supports. This gives a poorer mechanical operating commutator and can give inferior commutation even at reduced temperature rises.

The Rating and Application of Motors for Refrigeration and Air Conditioning

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Synopsis: This paper describes the procedure and practical results to date in applying single-phase motors to refrigeration compressors. The applications are primarily torque applications, temperature considerations being of secondary importance.

A line of high-torque motors has been developed to meet the high starting and accelerating torques demanded by the compressors. Utilization of these torques results in running loads considerably higher than rated name-plate loads, without exceeding safe operating temperatures for the insulation life requirement.

To meet these requirements a revised method of rating refrigeration motors on a starting and accelerating torque per horsepower and a starting efficiency basis is described. This method more clearly specifies a motor so that a more satisfactory application would be obtainable both from the standpoint of the user and the public utility company or code authority.

THE continued growth of mechanical refrigeration in the past 15 years has greatly increased the number of single-phase electric motors used. There are more motors in the range of one-third horsepower to three horsepower being manufactured and applied to refrigeration compressors than for any other use. Practically all of these are connected to the lines of the public utility systems, and their installation is subject to the rules of the National Electric Code, Underwriters Laboratories, etc., besides the rules of the public utilities. From the standpoint of the user, the initial and operating costs are important as well as dependability and quietness of operation. Since the horsepower rating and temperature rise as stamped on the name plate have grown to be a poor description of the motor, it is thought advisable to consider better ways of rating motors as applied to refrigeration compressors. Inasmuch as the starting and accelerating torques rather than the temperature rise at name-plate horsepower are the determining factors in the selection of a motor, it may be necessary to include these in the future standards. The purpose of this paper is to show how particular motor characteristics have come to be used and to suggest a possible means of rating this group of motors for the future.

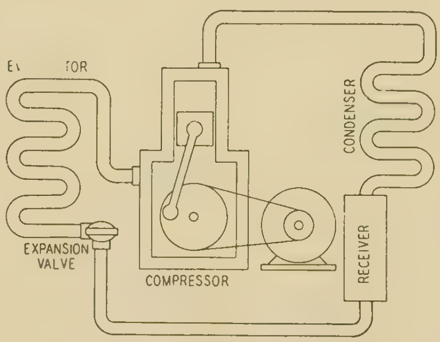

Figure 1. Diagrammatic sketch of refrigeration system

Selection of Motors

All mechanical refrigeration today is performed by vapor-compression machines. Figure 1 shows diagrammatically such a system. The liquid or refrigerant (usually Freon, methyl chloride, or sulfur dioxide) is alternately liquefied and vaporized. Refrigeration is produced by the latent heat of vaporization of the refrigerant. The vapor resulting from this vaporization in the evaporator or cooling element is drawn into the suction or low-pressure side of the compressor. The compressor then converts this low-pressure gas into high-pressure gas and forces it into the condenser. Here it is liquefied through cooling by means of water or air. The liquid refrigerant is then allowed to return to the evaporator through an expansion valve or restricted orifice.

The function of the electric motor in this system is to drive the compressor which compresses the low-pressure gas into high-pressure gas. The pressure in the crank case or at the low-pressure side of the piston is referred to as "back pressure" and the pressure on the high-pressure side or condensing side as the "condensing" or "head pressure." In a given system, the condensing and back pressures and the speed at which the compressor is driven are the determining factors in the amount of starting and accelerating torque required of this motor, as well as the operating load.

The motors used on these systems are the single-phase repulsion-start induction-run type in sizes of one-fourth horsepower, to three horsepower. Capacitor motors are beginning to be used in the one-fourth-horsepower, one-third-horsepower, and one-half-horsepower sizes but seldom little above these ratings because of the high starting currents compared with the other type motor. Practically all the refrigeration systems now using these sizes of motors are expansion-valve systems and the "pull down" is so short that it can be said to be no more severe on the motor from a temperature standpoint than the regular loads during cycling. By "pull down" is meant the first running period after installation or extended shutdown.

The application of the motor to the refrigerating unit is usually in the hands of the refrigeration engineer or a field installation engineer. Because of their desire to keep down the initial cost (as the cost of the motor is usually a large part of the cost of the condensing unit), the refrigeration engineer naturally will use all the available torque and carrying capacity of the motor at hand. In a refrigeration unit we shall show that the limiting load is not the running load, but starting and accelerating under the most adverse conditions. This is another way of saying that the motors are selected on a starting and accelerating torque basis rather than on a temperature rise at name-plate horsepower basis.

Suppose, for example, a new unit is to be manufactured. A compressor having a suitable bore and stroke based on previous experience of cost, etc., is designed and built. The condenser is selected in the same manner. The evaporator or cooling unit is designed to meet the demands of the load. This gives the engineer a unit on which to base his tests. As the majority of compressors are belt-driven, the speed is regulated by pulley sizes on the motor.

In any given unit the condensing pressure is a function of the effectiveness of the condenser. This pressure varies in the same manner as the pressure-tempera-

Paper number 39-18, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 30, 1938; made available for preprinting December 9, 1938.

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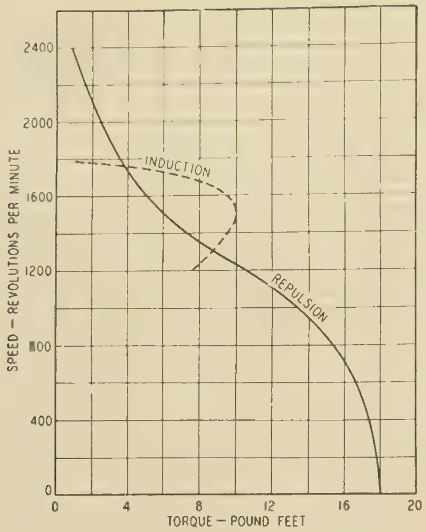


Figure 2. Speed-torque characteristics curve of one-horsepower repulsion-start induction-run motor

ture curve for saturated vapor for the refrigerant used. For various cooling-water and air temperatures that are likely to be encountered, and by a few tests on the effectiveness of the condenser, the range of condensing pressures can be determined. Further, by knowing the type of load on the system and the temperature of the evaporator a range of back pressures can be determined. With this range of condensing and back pressures defined the unit can be tested for motor requirements. A pulley is selected giving an average speed and tests are started.

For starting or breakaway in a given compressor the greatest torque required of the motor is at the condition of greatest difference between the condensing and back pressures. This condition occurs on certain types of loads and usually when the back pressure is very low. A pulley is then selected that will start the compressor under this worst condition at some arbitrary voltage limit from 10 per cent to 20 per cent under the name-plate rating.

This worst condition of condensing and back pressure usually occurs sometime during the running cycle and not after the unit has been off and is ready to start. If there is a momentary power failure and power returns there must be sufficient torque available to start the compressor.

The accelerating of the compressor requires a torque which is nearly proportional to the absolute back pressure. The pull-in or minimum accelerating torque is then the limiting factor in the amount of load the motor will accelerate. The high accelerating torque is necessary because of the work which is being done during each stroke of the piston. This work is the compression of the gas and it will be seen that with greater back pressure more gas is compressed during each stroke of the piston. Usually, it is found that the pull-in or accelerating torque of the motor is the limiting factor and not the starting torque. This is more noticeable on repulsion-start induction-run motors because of the high ratio of starting to pull-in torques.

Since the back pressure at which the unit runs is determined by the nature of the load, the range of back pressures will be great. If the evaporator or cooling coil is to cool an ice-cream cabinet the temperature of the coil will be relatively low, around ten degrees Fahrenheit. If the cooling coil is to be used for air conditioning, its temperature will be much higher, probably 40 degrees Fahrenheit. The back pressure in the former case is low and in the latter case high (assuming a given refrigerant). Since the accelerating torque is a function of this back pressure, there should be a theoretical best speed of the compressor for each back pressure. Since this is not practical the same compressor is usually provided with three different pulleys which are used for low, medium, and high back pressures.

Thus the refrigeration engineer has utilized all the torque of the motor in the application to the compressor. The last point he checks is the running load. For present-day compressors the running load when the torque is completely utilized is usually beyond the name-plate horsepower rating. Although a large number of cases require intermittent operation of the motor, the design of the whole unit from a temperature standpoint must allow for continuous operation of the motor.

For example, take a typical unit driven by a one-horsepower 110–220-volt 1,750-rpm 60-cycle single-phase repulsion-start induction-run motor. The speed-torque characteristic curve is shown in figure 2 and the running characteristics are shown in figure 3. The compressor was tested as in the procedure above and it was found that three pulleys for low, medium, and high back pressures were satisfactory, giving compressor speeds of 635, 570, and 510 rpm. The characteristics of the compressor are partly shown on figures, 4*a*, 4*b*, and 4*c*. Freon (*F*-12) is used as a refrigerant and a water-cooled condenser is shown. Similar data are available for the same compressor with an air-cooled condenser.

A typical application of the condensing unit would be made in a manner as described above. For selected values of 50-degrees-Fahrenheit refrigerant temperature and 70-degrees-Fahrenheit condensing water, the data given in table I appear.

The motor on the installation was found to accelerate the load at 90 volts on a 110-volt system. Satisfactory starting

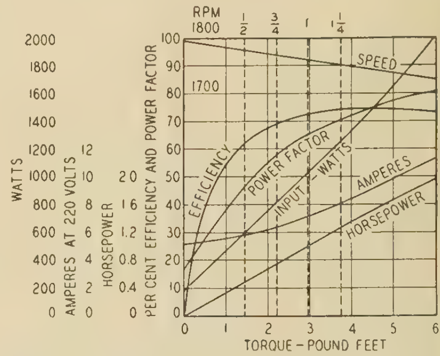


Figure 3. Running characteristics of one-horsepower repulsion-start induction-run motor

conditions were also assured. The temperature rise of the motor as measured by resistance at this load was 46 degrees centigrade in the rotor windings and 38 degrees centigrade in the stator windings. Thermocouple measurements for the same load gave 44.6 degrees centigrade and 40.4 degrees centigrade.

If, however, the load is increased because of an increase in the condensing water temperature from 70 degrees Fahrenheit to say 80 degrees Fahrenheit, the horsepower required rises to 1.57. At this new shaft load the temperature rise by resistance is 46 degrees in the stator windings and 54.5 in the rotor windings. At an increase in line voltage from 110 to 120 volts (which is very common in service today) the temperature rise at 1.57 horsepower load will be 58 degrees centi-

Table I

Condensing Water (Deg F)	Btu per Hour Compressor Output	Back Pressure (Pounds per Square Inch)	Motor Input (Watts)	Motor Shaft Output (Horsepower)	Temperature Rise by Resistance (Deg C)	Temperature Rise by Thermocouple (Hot Spot) (Deg C)
70	23,500	46.7	1,395	1.40	46 (rotor)	46.6
					38 (stator)	40.4
80	22,600	46.7	1,530	1.57	54.5 (rotor)	53.3
					46 (stator)	48.9

grade by resistance and 60 degrees centigrade by thermocouple in the rotor windings. (All the thermocouple values are the hot-spot values obtained from a large number of locations.)

The temperature rise of 58 degrees centigrade continuous at the 1.57-horsepower load does not seem excessive because the load is a maximum. The load on the system cannot be changed unless the temperature of the condensing water rises. This is an ideal application of the motor however, as the user is getting the most refrigeration per dollar from the installed apparatus because he is using all the torque available in the motor and because the running point is at the point of maximum efficiency of the motor.

There are of course, a large number of other considerations in the selection of the motor for this refrigeration system. The starting current of the motor should be low enough to comply with the regulations of the public utility systems. The noise level of the whole system and particularly of the motor must be low as the installa-

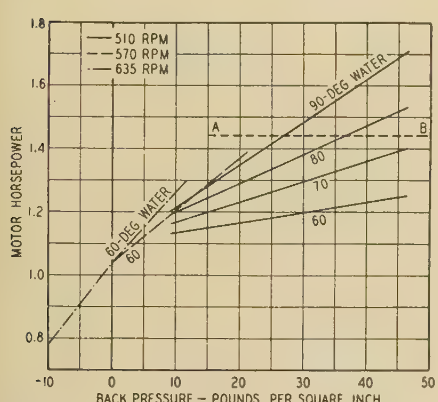


Figure 4a. Condensing-unit characteristics

A-B—Line of maximum loading

tion is made where quietness is necessary. The magnetic noise of motors for refrigeration units has been given considerable attention. Although a design which gives low magnetic noise is essential, it has been found that improvements of manufacture to hold the air gap concentric have greatly assisted in producing a consistently quiet product. Due to the nearly universal use of V-belt drive it is necessary to build motors with end-play takeup or cushioned for end-bump. In general, the large production of refrigeration motors has helped the motor manufacturer to improve the quality and decrease the cost of motors in these sizes. The characteristics and ratings of motors used on refrigerating systems, because of the large quantity of motors used for this purpose, have greatly influenced the characteristics and ratings of other motors. It is therefore logical to discuss the ratings of motors as used on refrigeration systems.

Motor Ratings

With the use of the torques and overloads described above, other problems are introduced. The horsepower marking along with the corresponding full-load current as placed on the name plate are used by various code authorities for selecting the proper wiring and fusing. The control engineer uses these currents in selecting the proper overload control. The public utility uses the horsepower rating as stamped on the name plate to set the maximum allowable starting current. Further, the whole electrical industry refers to the motor in terms of the marked horsepower. However, a newer use of the horsepower markings has arisen in the refrigeration industry and that is the naming of the refrigeration unit by the horsepower of the motor used on it. This latter practice transfers the variations of compressor applications, designs, and inequalities of the whole system to the motor when comparing various units.

Gradual development and improvement of design and manufacture of refrigeration motors without an increase in the physical dimensions has further

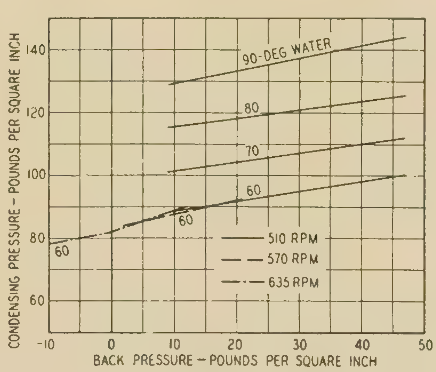


Figure 4b. Condensing-unit characteristics

raised the torque obtained from a given quantity of materials without increasing the name-plate rating. It is felt that here we have a typical case of a motor rating based on carrying capacity at full load as now shown by AIEE Standards, not coinciding with actual practice.

When we suggest formulating a new method of rating for these motors, we immediately enter new territory.

It is felt that a service factor is a makeshift way of showing the overload of these motors. It is not all-inclusive in that it does not give a true picture of the higher starting and accelerating torques and does not allow for starting current. It is felt that too much education of the code authorities would be necessary to get the proper wiring and fusing to accommodate the increased load without the name-plate stamping being accordingly altered.

It is pointed out that for refrigeration applications the required life of the motor seldom is greater than 15 years. Usually, in this length of time the unit is replaced by more up-to-date equipment. The running time for the average unit on a refrigeration application is much less than full time and actually nearly half time. On an air-conditioning unit the running time during the year is one-fifth to one-fourth of the total time. There are also a great number of cases where the on cycle for a large part of the life of the unit is so short that the motor does not reach its ultimate temperature during each cycle. The low ambient temperatures usually encountered give a further factor of safety for motor temperatures. An ambient temperature above 90 degrees Fahrenheit is recognized by a suitable decrease in compressor ratings and motor loadings by the refrigeration unit manufacturers. Further, on air-cooled condensing units the use of the fan on the motor shaft extensions to pass air through the condenser, usually cools the motor in addition to the fan within the motor itself. Therefore, on an insulation-life basis we have an excess of useful life of the insulation for a hot-spot temperature of 105 degrees centigrade. A further important point to consider is that the difference between the hot-spot temperature as measured by thermocouple embedded in the windings on motors of these sizes and the temperature as measured by an adjacent thermometer is very little. This leads us to the suggestion that for refrigeration applications, motors be rated 50 degrees centigrade. As the published tables of the refrigeration manufacturers show the

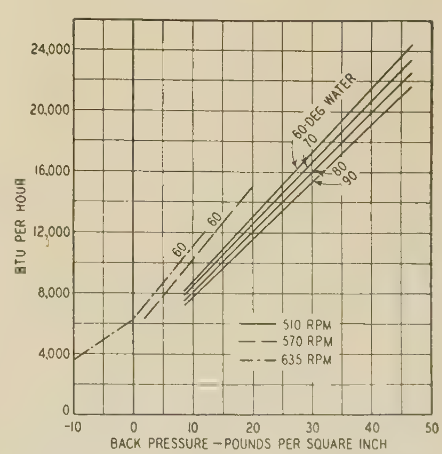


Figure 4c. Condensing-unit characteristics

permissible overloading, and as this overloading is not allowed beyond a point where there will be a temperature rise greater than 50 degrees centigrade, there would be no need for a service factor if the horsepower of the motor used has a 50-degree-centigrade rise as its name-plate stamping. If 40 degrees centigrade rise is used by the electric industry as standard, the refrigeration engineer will still load his motors to a 50-degree-centigrade rise and we will have loads out of line with name-plate ratings.

Specifying the temperature rating of motors used on refrigeration systems will not accurately define the motors as used today. This alone will not stop the practice of having a horsepower stamping on the name plate different from the actual horsepower load on the motor. However, since the motors applied and used on a torque and temperature basis and from experience over the past years torque values have been found which are satisfactory for refrigeration applications it is entirely possible to specify for motors on refrigeration applications the following:

(*a*). The accelerating torque per horsepower

(*b*). The starting torque per horsepower

(*c*). The starting efficiency in pound-feet per ampere locked-rotor current

The curves in figure 5 show the accelerating and starting torque per horsepower as found today. If, however, another curve of torque per useful horsepower is drawn, it will coincide closely with the torque per horsepower if the horsepower ratings were all moved up to the next higher horsepower ratings.

Figures 6*a* and 6*b* show the locked-rotor currents per horsepower for the repulsion-start induction-run and capacitor type motors. In figure 6*a* the currents are taken from present-day motors and based on the horsepower stamping on the name plate. Figure 6*b* shows the locked-rotor current per horsepower for the proposed name-plate stamping. For the repulsion-start induction-run motors the horsepower used was the useful horsepower as shown in figure 5 which is practically the same as the next higher horsepower rating. For the capacitor motor, the horsepower used was that obtained from present motors when rated 50 degrees centigrade rise. These starting currents per horsepower would be obtained if the present motors were designed for 50 degrees centigrade rise. Figure 6*c* shows the starting efficiency in torque per ampere locked rotor current. If then, 50 degrees centigrade rise is accepted the torques and starting efficiency could be selected as follows:

5.5 pound-feet per horsepower minimum accelerating torque

11.0 pound-feet per horsepower starting torque

0.5 pound-foot starting torque per locked ampere at 220 volts

As was pointed out previously, the starting torque available on the repulsion-start induction-run motor was usually much higher than necessary and the limiting torque of the motor on nearly all applications is the minimum accelerating torque. With the capacitor-start capacitor-run or capacitor-start induction-run motor this situation is not the same. Here the starting torque is less and the accelerating torque per horsepower is greater. See figure 7 for a speed torque curve of a capacitor motor. The starting torque per ampere or starting efficiency is much less. At the present time capacitor motors are not used in many places above three-fourth horsepower but from the data available the torques are as follows:

5.5 pound-feet per horsepower minimum accelerating torque

10.0 pound-feet per horsepower starting torque

0.3 pound-foot starting torque per locked ampere at 220 volts

It is therefore felt that here we have torque values which specify the motor and which can be tied up with the proposed name-plate rating. They are satisfactory from the standpoint of the user, the starting currents agree with those required by the public utilities, and the control engineer can select proper control within the limits of the National Electric Code. These also prevent the false marking of name plates.

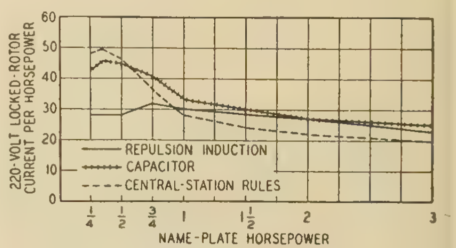


Figure 6a. Starting currents per horsepower for present-day motors

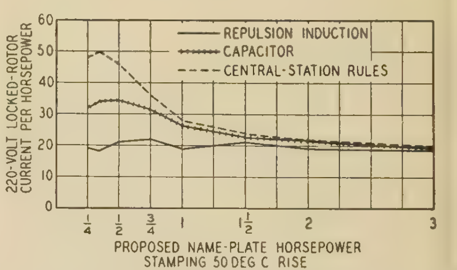


Figure 6b. Starting currents per horsepower for proposed 50-degree-centigrade motors

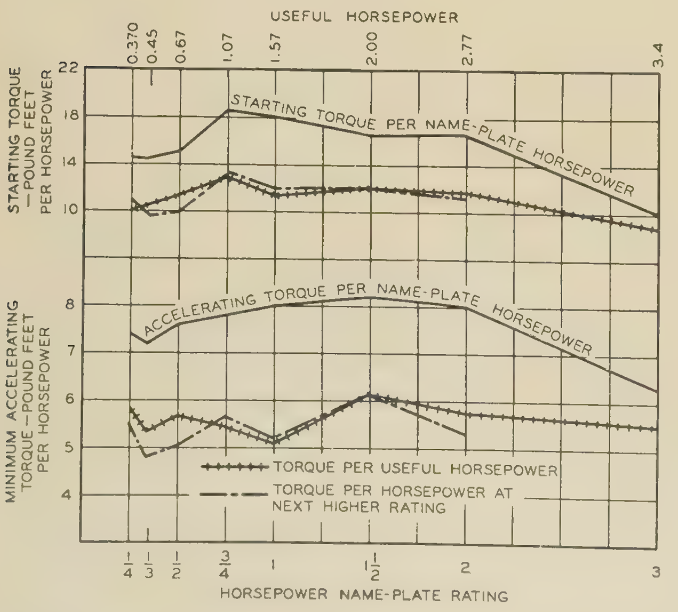


Figure 5. Starting and accelerating torques for various horsepower ratings

It may be argued that specifying of the torques per horsepower will tend to limit new developments or improvements in the present motors. There is obviously nothing to prevent the engineer from increasing the starting or accelerating torque per horsepower if there is no increase in starting current. There is also nothing to prevent the engineer from increasing the horsepower carrying ability of a certain motor if he does not change the torque relations as set.

The increasing use of hermetically sealed compressor units makes the motor

ratings for these units a pertinent subject. The temperature rise of the motor here is dependent upon the refrigeration manufacturer. If, however, the horsepower ratings are not set on a similar basis of torques per horsepower the same bad practice of false name-plate stamping will creep into the ratings of these motors. It is not the purpose of this paper to state what these should be at the present time.

It will, of course, be argued by some that this basis of rating may be satisfactory for refrigeration applications, but not for others. From the standpoint of the

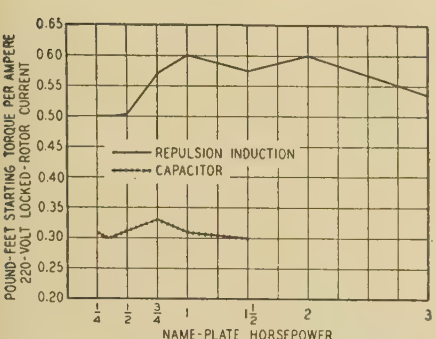


Figure 6c. Starting efficiency for capacitor and repulsion-start induction-run motors

public utility company the starting current per horsepower could not change and if less starting torque is necessary there would probably be a decrease in starting current.

It must always be kept in mind that standards or ratings set by the electrical industry should allow the large-scale user to get the fullest possible use of the motor for the requirements peculiar to its application. If the standards do not allow this, large users of motors will either break the standard or manufacture motors for their own use.

Conclusion

It has been shown that in the application of refrigeration motors, the accelerating torque and starting torque consistent with starting-current limitations with a 50-degree-centigrade temperature rise are the determining factors.

The recommendations for setting values of starting torque per horsepower, accelerating torque per horsepower, and starting efficiency in torque per ampere of locked-rotor current offer a logical and definite basis of rating for these motors. These tie together the present two types of high-torque single-phase motors so that the same horsepower rating on the name plate can be used on both for any given compressor application. These also offer a method of rating motors for use in hermetically sealed units which are increasing in use for all sizes of refrigerating units.

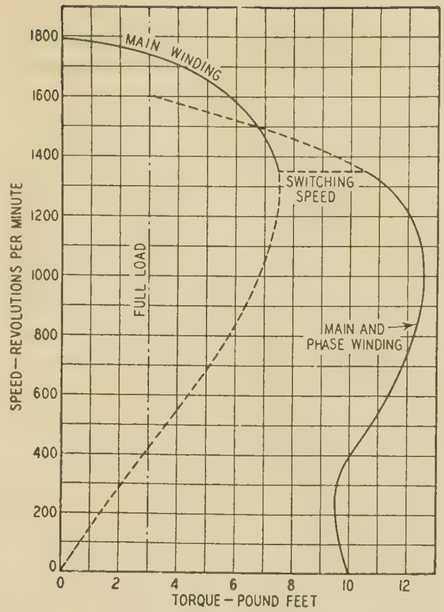


Figure 7. Speed torque characteristics curve for one-horsepower capacitor-start capacitor-run motor

Discussion

D. F. Alexander (nonmember; General Motors Corporation, Dayton, Ohio): This timely paper outlines an application problem which should be of great interest to those who design or create standards for small single-phase motors. The refrigeration and air-conditioning industry has become a larger user of these motors, and has special requirements which the familiar general-purpose motor does not meet. Manufacturing competition has forced an economy of motor materials to suit the job at hand, and the resulting line of motors calls for a reconsideration of existing rating standards.

It has been apparent in this field for several years that motor name-plate markings and application codes are out of line with practice. This has handicapped the manufacturer, the salesman, the contractor and the purchaser, not to mention the power companies and the sponsors of the National Electric Code. Attempting to find a suitable compromise among the various codes and standards now in force, it appears that all these interested parties have cause for complaint.

The selection of proper controls, wiring overload protection, and fusing for these motors is often based on the name-plate horsepower and current, despite the fact that such markings today give no indication of the actual motor overloads. Wiring and fusing are often inadequate, or must be replaced by the contractor before code authority approval can be obtained. Summing up, we have today a varying-duty motor load often far in excess of the motor rating, with fictitious name-plate markings. A revision is in order, to bring the marking more nearly in line with the facts. As Mr. Rutherford points out, a revision of these motors to a 50-degree-centigrade rise basis, from the present 40-degree-centigrade basis, would go far to remove the present confusion in this respect. In addition, suitable wording might appear on the name plate to distinguish these motors from the general-purpose type.

The paper describes the steps necessary in selecting a motor for a refrigeration compressor. You will note that the torque requirements, in general, determine the motor design, and not the temperature-rise at name-plate horsepower. For that reason, it would be preferable that the motor name plates be rated in terms of torque and starting currents. This is not practical, since the horsepower rating is the common term for identification of the complete unit as well as for the motor. As the next best thing, then, let us specify torque and current limits for the 50-degree-centigrade-rise refrigeration motor, in the manner described in the paper. It has been suggested elsewhere that a new series of service factors for 40-degree-centigrade motors be used, rather than a straight 50-degree-centigrade rating. This may be satisfactory for the motor manufacturer, but for those who must select control equipment and wiring for which there are no service factors, the problem might become more complicated than at present. The use of such service factors for *polyphase* motors five horsepower and larger may be justified, but does not come within the scope of these comments.

The original basis for selecting 40 degrees centigrade rise for general-purpose motors did not allow for an application such as this one. A number of important factors permit a higher temperature rise, as listed in the paper. New information on the life of class *A* motor insulation, as reported in other papers at this convention, leads me to believe that a 60-degree-centigrade rise rating for refrigeration motors might not be excessive. However, we must not overlook the effect of high temperatures on motor-mounted capacitors, the excessive copper losses due to the cumulative effect of heating at prolonged overloads, or the danger to the lubricating system. Severe overloads in some motor designs will also result in poor speed regulation. Everything considered, and noting successful experience during the last few years at 50 degrees centigrade rise, it would seem that this value would be best.

With the spread of commercial refrigeration and of air-conditioning units, the next few years will see a large increase in the use of single-phase motors from one-third to three horsepower. In this connection, it would be profitable to note the progressive and realistic attitude toward motor ratings as expressed in Mr. Hellmund's paper (AIEE TRANSACTIONS, volume 58, 1939, pages 499–503). It would be very helpful if any changes in rating could be made promptly.

L. C. Packer (Westinghouse Electric and Manufacturing Company, Springfield, Mass.): This is an excellent paper outlining the application of single-phase motors for refrigeration compressors and the au-

thor's recommendations for rating of motors for this type of application are of considerable interest.

In the application of single-phase motors to refrigerating compressors, there are, as the author states, many things to consider.

For instance most domestic refrigerators use the hermetically sealed type compressors, using split-phase-type induction motors, whereas the commercial-type refrigerator units use both belt-driven and hermetically sealed type compressors.

The belt-driven type used in most cases uses the repulsion-type motor. The hermetically sealed type, as a rule, uses a capacitor start and run motor from approximately 1/4 horsepower to 1 1/2 or 2 horsepower. Small air-conditioning compressors of the hermetically sealed type use capacitor motors of a little higher rating.

As the author states in his paper, it is essential that the motor have sufficient accelerating torque so that it will not pull out at the maximum back pressure and discharge pressure at the lowest expected voltage.

The starting torque required depends a great deal upon the type of compressor. The starting torque of a repulsion motor is greater than split-phase or capacitor-type motor; therefore it is adaptable in most cases for belt-driven-type compressors. It is well to have reserve starting and accelerating torque in this case to take care of cases where the belt may be tightened too much.

In the hermetically sealed compressor, which is gaining prominence rapidly, the starting torque required, at the lowest expected voltage, depends upon whether or not it is desired to use an unloader to equalize the pressures, before restarting after stalling. It is the designer's duty to get the best starting conditions at the lowest cost of the equipment and at the lowest possible starting current. Also keep in mind that he wants the highest possible efficiency with the necessary accelerating torque.

Assuming that no unloader is used. The motor of capacitor type must have sufficient capacity in the auxiliary-winding circuit to get the starting torque required. However, sufficient torque may not be obtained in some cases without impairing the performance under running conditions because the maximum starting torque is obtained when the reactance of the starting capacitor is equal to the reactance of the auxiliary circuit. The current in the auxiliary winding and the capacitor is also the maximum under this condition. If this condition does not give sufficient starting torque it does not pay to increase the capacity beyond the point where the reactance of the capacitor is less than the auxiliary winding reactance, as the starting torque starts to decrease because of the decreasing current in this phase and smaller phase angle between the main and auxiliary windings. The designer will then most likely put in an unloader to equalize the pressures. The starting torque required in this case is much less than without an unloader and the motor will require less capacity; in most cases at a saving and at a slightly lower starting current. It is even possible in many cases to use a single capacitor for both running and starting, thus effecting a further saving.

In the case just cited, the minimum cost is obtained which is important to the customer and the starting current satisfies the requirements of the power companies. Therefore, there is nothing to be gained by holding to a standardized ratio of starting torque to horsepower. As long as the starting torque is sufficient, it would be less costly to keep this ratio as small as possible.

The ratio of starting current per foot-pound starting torque would be higher than the proposed standard in this case, but as noted above no higher torque is necessary. While the capacitors in the circuit have some influence on the line current at starting, the current in the main winding at starting is definitely established by the accelerating torque required. This current is very nearly equal to the line current and in some cases greater than the line current. Therefore, the ratio of starting torque per ampere has very little meaning.

The author suggests a 50-degree-centigrade temperature rise. This apparently is a debatable question and may apply to motors for belted applications and might also come within the suggested values by Messrs. Alger and Johnson in their paper on "Rating of General-Purpose Induction Motors" (AIEE Transactions, volume 58, 1939, pages 445–59). However, in hermetically sealed compressors, there are, as noted before, many conditions to meet. fifty degrees centigrade rise is too high with some types of permissible insulation and in some cases the higher temperature is too high for other reasons. There are some cases where the transmission of motor heat does not increase with load. There is then no definite relationship between temperature rise and horsepower. Thus, it appears that to set up a standard of rating motors for refrigeration and air-conditioning compressors without careful consideration of the problems of hermetically sealed units, may handicap the manufacturers of this class of apparatus with no advantages to the user. Of course, no one has to design the motor for a 50-degree-centigrade rise. But without some basis there is nothing definite from which to establish the horsepower rating.

Messrs. Alger and Johnson noted in their paper just mentioned that temperature rating standards do not apply to this class of motors. There is considerable merit to this thought, as well as to adhering to breakdown torque and starting-current rules, but the ultimate user may be paying in efficiency for a breakdown torque that is not required. Thus in the case of hermetically sealed compressors the application can be satisfactory with breakdown torque and starting torque below suggested values for a standard, resulting in economy in the size of the motor and yet with satisfactory starting current. Rotary-type compressors will require special studies concerning the application of motors to them.

Therefore, where hermetically sealed units are concerned, if the name plate is stamped with the horsepower and load current representing some standardized working load, and in line with the starting current rules, the desires of National Electric Code, Underwriters and power companies, and control engineers will be met. The name-plate current, however, may not tie up with the horsepower rating because auxiliary apparatus, fan motors for cooling, etc., will take some of the current.

R. A. Fuller (General Electric Company, Fort Wayne, Ind.): Experience with the application of motors to refrigeration and air-conditioning compressor drives indicates that the limiting factor will be one of the following:

1. Maximum continuous permissible load
2. Starting torque
3. Maximum, or pull-out torque
4. Accelerating torque

Repulsion-induction motors tend to be limited by accelerating torque or maximum continuous permissible load. The principal limiting factors for capacitor motors are starting torque and maximum continuous permissible load. Only in the one-sixth and one-fifth horsepower ratings has there been any indication that torques alone can be used for rating purposes.

Polyphase motors tend to be limited by maximum continuous permissible load, starting torque, or accelerating torque.

D-c motors tend to be limited only by maximum continuous permissible load.

It is therefore believed that the proposed method of rating has possibility of general application only in the fractional-horsepower single-phase ratings and further that its general application there is somewhat questionable.

Compressor designs enter into the torque requirements. For example, a four-cylinder compressor will tend to have a starting torque requirement approximately the same as a two-cylinder compressor of one-half the capacity. Thus, the construction of the compressor will have considerable influence in determining the limiting factor in the particular motor application.

The refrigeration engineer has considerable test data and empirical procedure on motor torque requirements and motor loading. Fundamentally, however, motors are still applied to these refrigerant condensing units by cut and try methods. The technical facilities of the present day should permit us to superimpose a motor speed-torque curve on a compressor speed-torque demand curve and thus accurately and readily determine the suitability of the motor. Work already done along these lines has shown some promise.

It is suggested that the torque elements of these applications might best be met by comparing speed-torque curves of the motors with speed-torque demand curves of the compressors. It is recommended that any rating method should include maximum continuous permissible load.

A. F. Lukens (General Electric Company, Lynn, Mass.): Mr. Rutherford has brought out the essential points of applying motors to refrigeration and air-conditioning compressors. The more important points are:

1. Motor size is determined by starting and pull-up torque rather than motor heating.
2. The pull-up torque is usually more important than starting torque.
3. Motor noise and end-bump.

He also states that capacitor motors have not had wide use in the sizes above three-fourths horsepower.

However, integral-horsepower capacitor motors can be designed that meet the re-

quirements pointed out by Mr. Rutherford of high pull-up torque, quietness, and end-bump and in addition have the advantage of high efficiency and power factor and high maximum running torque. The last is important as it prevents lowered efficiency and light flicker brought about by overloads, undervoltage, or inadequate flywheel, or the combination of the three.

Tests of integral-horsepower capacitor motors on compressors of different manufacture have proved that 300 per cent starting torque is ample, and that more than this is unnecessary. This checks Mr. Rutherford's twice repeated statement that for the repulsion-induction motor the pull-up torque and not the starting torque is the limiting factor. In other words, the repulsion-induction motor has an excess of starting torque.

Since pull-up torque and starting current are the limiting factors in starting ability, it is suggested that the third specification, namely, "the starting efficiency in pounds-feet per ampere locked-rotor current" be changed to the ratio of pull-up torque divided by locked-rotor amperes. This value is then a measure of the starting efficiency of the motor.

The pull-up torque of a well-designed capacitor motor should be greater than 200 per cent of full load torque on the present basis of rating and can be 220 per cent. The starting current of the capacitor motor is not more than ten per cent greater than the repulsion-induction motor as shown by figure 6. Using the most adverse values the ratio is 0.25 foot-pounds pull-up torque per locked-rotor ampere for both types of motors.

The application of motors to use the full extent of their torque ability obviously reduces the factor of safety of the motor under load, so that it is felt that protection of the motor should be supplied. An automatic reset overload relay actuated at least in part by line current provides excellent protection under all conditions of abuse without completely stopping the refrigeration. Such a device prevents damage to the motor under severe overloads without completely interrupting the refrigeration which in turn prevents wholesale spoiling of the refrigerated product. In the case of failure to start, the device removes the motor from the line for a time long enough for it to cool down before restarting. In the meantime, the head and back pressures have had a chance to equalize and the starting duty is easier. This "limping" operation of the compressor will eventually come to the attention of the user and can be fixed without total loss of refrigeration.

Chester Lichtenberg (General Electric Company, Fort Wayne, Ind.): Standards for electrical machinery and particularly electric motors may be grouped according to usage.

Dimension standards, including frame sizes, afford design convenience. They present a hazard since any design restriction hampers imagination and progress. Design standards may be developed by individual groups of engineers promptly responsive to new conditions, but do not appear to have a place in industry standards sponsored by trade associations such as National Electrical Manufacturers Association, Radio Manufacturers Association, and American Cotton Manufacturers Association.

Application standards are essential for the effective and economical usage of apparatus including electric motors. Application standards might include items such as maximum starting torque, maximum starting current, and rotor flywheel effect. These might well be included in trade association standards since they affect the manufacturer, the buyer, the user, and the public utility. Recommended practices or adopted standards for these and similar items would be genuinely helpful.

Rating standards, however, are broader in usage, more fundamental in concept, and should be more rigidly defined than either design or application standards. They should be so fundamental that they will accurately reflect wide varieties of designs and application. They should be so simple that ordinary folks can use them. They should be so definite that all users will understand them. They must be unbiased and therefore the result of joint deliberations. Hence rating standards for electrical machinery, as distinguished from design standards and application standards, are a unique standardizing function of the AIEE and the refrigeration industry as an example is watching, certain that the AIEE will do a good job of rating standardization.

B. M. Cain (General Electric Company, Lynn, Mass.): Every engineer recognizes that the approach to an application problem should be based largely on the requirements of the load without preconceived ideas as to the type of drive or its possible limitations. By such an approach the limitations of available types of drive can be clearly and carefully analyzed.

Mr. Rutherford has clearly demonstrated that for the refrigeration compressor there is a definite ratio between the accelerating torque and the torque at average load. He has also shown that the refrigeration engineer applies the motor largely on the basis of its ability to provide the accelerating torque required by the load. The only other essential requirement is that the motor must not fail due to overheating.

Tests have shown that the ratio of accelerating torque to useful running torque of a compressor is about 1.65. This means that, after allowing for ten per cent low voltage, the pull-up torque of the motor must be 1.65 divided by $(0.90)^2$ or 204 per cent.

An extra safety factor, advisable in accelerating torque to guard against a long accelerating period, brings this to about 220 per cent.

The breakaway torque of compressors is shown by test to be about 50 per cent greater than the torque at 80 per cent speed. Thus the starting torque of the motor must be 204 per cent $\times$ 1.5 or about 300 per cent. No safety factor is needed.

The torque requirements of any driving motor have thus been established at:

1. Pull-up torque—about 220 per cent of full-load torque.
2. Locked-rotor torque—about 300 per cent of full-load torque.

Any torque in excess of these values will not be useful and conversely any motor having less than these values cannot, in most cases, develop its useful capacity.

Examination of the speed-torque characteristics of various types of single-phase motors shows that for these starting and accelerating torques a limitation of starting current will always limit acceleration torque and not starting torque.

The minimum accelerating torque of well-designed capacitor motors or repulsion-induction motors is substantially the same. It can be conservatively as much as 0.25 pound-feet per ampere of locked-rotor current at 220 volts. This means that the locked rotor current of motors for driving compressors must be $1 \times 5{,}250/1{,}725 \times 2.20$ divided by $0.25 = 26.9$ amperes per horsepower.

That is, regardless of type, the starting current must be at least about 27 amperes per horsepower in order for the motor to carry its useful capacity.

By limiting the starting current to less than this amount the user is forced to sacrifice useful capacity which he has paid for on a horsepower basis.

On the other hand extending the starting current limit to higher values enables the manufacturer to pass on to the user economies in cost and performance obtainable with higher starting currents.

Recognition of this fact by some of the leading power companies has already started the trend toward higher allowable starting currents.

C. G. Veinott (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Mr. Rutherford is to be complimented for presenting a useful and educational paper. He gives data on the application of motors to refrigeration and air-conditioning apparatus, thereby showing the underlying reasons for the specifications set up by this class of manufacturer.

His paper brings out into the open a fact long recognized by those familiar with this application of motors—namely that the horsepower rating stamped on the name plate does not adequately describe the capacity of the motor since the latter frequently carries from 30 per cent to 50 per cent overload continuously. The severity of this service is enhanced by the fact that the motors have to be able to carry these overloads in ambient temperatures appreciably above the established standard of 40 degrees centigrade. Thus, a one-half-horsepower motor used in refrigeration service is really a three-fourth-horsepower motor.

The author proposes to increase the horsepower stamping on the name plate to a value more nearly commensurate with the useful horsepower that the motor will develop, at the same time increasing the rated temperature rise from 40 degrees centigrade to 50 degrees centigrade. Without a doubt this honest straightforward proposal has unquestioned merits. By also increasing the ampere stamping to correspond with the new horsepower rating, much confusion among the control people and code authorities who specify the wiring, will be eliminated. Such a method of rating is more in accord with the objectives of an ideal rating structure outlined by Mr. Hellmund ("The Rating of Electrical Machinery and Apparatus," AIEE Trans-

actions, volume 58, 1939, pages 499–503). Unfortunately, however, this proposal may be opposed by the refrigeration manufacturers, particularly since some of them name their unit after the horsepower rating of the motor. Moreover, some of them seem to feel that, for a given unit of any size, the smaller the horsepower rating on the motor name plate, the more efficient the unit from a thermodynamic standpoint.

Also, if Mr. Rutherford's proposal were accepted, the motor manufacturers would have to be particularly vigilant to see that the refrigeration manufacturers didn't revert to their previous habit of applying overloads of 30 per cent to 50 per cent.

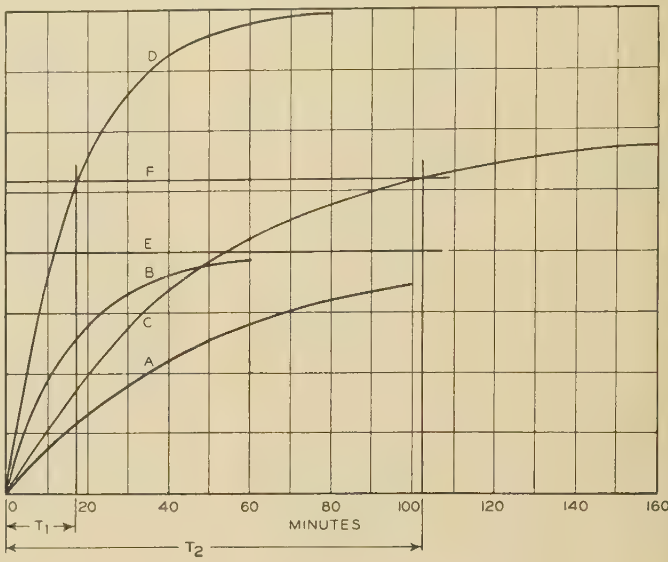
The author brings out the importance of adequate torques. He suggests certain torque specifications which are conservative and in accordance with present commercial motors. Why he specifies torques in terms of "pound-foot per horsepower" is rather puzzling. The present accepted method of specifying torques is in per cent of full-load torque. When specified in per cent, the figure becomes independent of the units used for measuring torque ("ounce-feet" is commonly used for fractional-horsepower motors and "pound-feet" for integral-horsepower motors) and is more nearly the same figure for motors of odd frequencies or different numbers of poles. Does Mr. Rutherford claim some unusual advantage for his new method of specifying torques?

Discussing the importance of locked-rotor amperes, the author proposes to specify them in terms of starting torque. If locked-rotor amperes are to be specified in terms of torque, we believe, concurring with Mr. Lukens, that they should be specified in terms of pull-in torque, which is generally the limiting torque, as pointed out by Mr. Rutherford. However, I believe locked-rotor current should be specified in amperes as at present; involving this specification with a value of torque can only be confusing to the power companies.

R. E. Hellmund (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): In Mr. Rutherford's paper and other papers,[1,2] motor applications are discussed in which the motor intermittently operates at relatively high load with prolonged intervening periods of no load or standstill. If in such cases the idle periods are relatively long compared with the operating periods, the root-mean-square load is, of course, well below the running load. On the other hand, the requirements for starting, pull-in, and pull-out torques are governed essentially by the character of the running load and therefore may have little relation to the root-mean-square load or to the continuous rating of the motor. Thus the latter obviously loses some of its practical significance and suggests the idea of making the pull-in, pull-out, and starting torques a more prominent part of the rating structure. However, while the conventional continuous rating in some cases loses in value on account of this, the motor temperature nevertheless is an important limiting factor in the operation of the motor. For this reason and also because rating by temperature-rise is so firmly established, it seems desirable to retain the temperature limits in some way or other as the basic factor for rating, but to supplement them by additional provisions for torque and starting-current values. Another reason for retaining temperature limits as a basic factor is that they are common to nearly all types of electrical apparatus, including generators, transformers, control devices, etc., while torques are of particular significance with motors only. It is believed that neither Mr. Rutherford nor the authors of the related papers previously referred to will disagree with this point of view, but their remarks might be misinterpreted because they have outlined methods for the application and selection of motors, in which, for practical reasons, the torque values are given consideration first and the temperature limitations are checked subsequently.

For some applications of motors, provisions for larger ratios of pull-in, pull-out, and starting torques have been made in the past and have been found to be a simple way out and one that is usually understood by everybody because it does not change the well-established basic method of rating but merely uses somewhat different ratios for certain torque values. When the load cycles are short, so that neither the motor nor any of the wiring or auxiliaries have a chance to vary much in temperature during the load cycle, there apparently is no reason for changing this well-established practice. However, there are some applications of motors with longer load cycles where the established practice would be satisfactory for the motor but where the heating effects might prove to be harmful to other parts of the circuit having a smaller time constant than the motor. This can best be illustrated by reference to figure 1 of this discussion. The condition shown here may be representative of a practical case of compressor equipment starting cold and requiring a heavy load during a prolonged pull-down period after installation or an extended shutdown such as referred to in Mr. Rutherford's paper. In the circuit of such a motor, there is always some likelihood of some parts (either the control or the wiring) having a much smaller time constant than the motor. In figure 1, curve A represents the time-temperature curve for the motor, while curve B is intended to apply to some part in the circuit or control having a much smaller time constant than the motor. In this case it has been assumed that during continuous operation both curves A and B reach a temperature-rise which is safe for continuous operation, as indicated by line E. It is further assumed that both the motor and the other circuit parts can be operated safely for short periods at a somewhat higher temperature,

Figure 1



as indicated by line F. If we now assume that the load during the pull-down period is about 40 per cent higher than the rated load, it may well happen that the total motor losses, which are composed of constant losses and I^2R losses, will be increased by about 50 per cent, thus resulting in a time-temperature curve C. On the other hand, the other parts in the motor circuit represented by curve B may be heated by I^2R losses only, which with the assumed overload would then about double, and, furthermore, because of the small time constant of such parts, corresponding temperatures will be reached quickly, as indicated in curve D.

Examination of the figure shows that there are three distinctly different conditions. If the pull-in period is less than T_1, or about 18 minutes in this particular case, neither the motor nor other parts of the circuit will reach unsafe temperatures and no special precautions or change in practice seems necessary. If the pull-in period is very long, exceeding T_2, or about 103 minutes, both the motor and circuit parts will reach unsafe temperatures, which simply means that larger apparatus of higher rating has to be applied. If the pull-in period is between the values T_1 and T_2 shown in the figure, the application is perfectly safe for the motor but not for some of the circuit parts. Naturally, if it is desired to obtain maximum economy, the motor of the smaller rating can and should be retained, but the wiring or control apparatus, or both, for a motor of a higher rating will have to be used. This is the condition referred to in Mr. Rutherford's paper which is of interest not only to motor engineers but also to engineers interested in the power supply and auxiliary apparatus. It is for such conditions that it may be

advisable to give the motor a short-time or intermittent rating higher than its continuous rating, as this would then automatically lead to the proper selection of conductors and auxiliary apparatus.

The advisability of establishing intermittent ratings has, of course, been considered frequently, but so far no standard methods of rating along these lines have been established in the United States. As pointed out by Rutherford, the rather broad field of applications for refrigeration and air conditioning, makes a review of this problem in connection with standardization highly desirable if the best and simplest method for handling these applications is to be selected. Further study will be necessary to determine which method is best suited for this purpose and whether the standardization of certain intermittent ratings is more desirable. The application of motors in railway work has always been one of the most important examples of motors for intermittent operation, and here the problem has been solved in a practical way by assigning both a short-time (one hour) and a continuous motor rating. In view of the necessity for close application of apparatus in railway work, various methods for applying railway motors have been worked out. Therefore, it may be advisable to follow railway practice as closely as possible in some other applications. The proposal in a paper by Hildebrand[2] comes rather close to railway practice. Incidentally, a review of the methods of rating for intermittent loads seems timely, not only on account of the increased use of compressors for refrigeration, air conditioning, etc., but also because economic conditions surrounding these applications are different from those applying to other uses of motors for intermittent loads in industry. In many industries, the cost of motors and their power consumption is a very minor part of the total operating cost of such industries. Therefore, industrial engineers were perhaps inclined to do some overmotoring in doubtful cases because it gave them extra safety margins, which often were more important than maximum economy in first cost and operating costs. Contrary to this condition, the first cost and operating costs of refrigerating and air-conditioning equipment are an important factor to the owners of small commercial establishments or of homes, and in order to broaden the field of application, more attention may have to be given to economies possible.

While the condition illustrated in figure 1 may seem alarming, little trouble has been experienced in the past from overheating of circuit parts. Very likely the designers of control and other circuit devices have simply strengthened certain bottleneck parts whenever there was any indication of overheating in actual service. Some of this was perhaps also made necessary by the starting conditions of squirrel-cage motors. In other words, the co-ordination of the various parts of circuits has in the past been carried out successfully by providing rather liberal margins. If closer and more economical application of apparatus is to be accomplished, a more systematic co-ordination of standards will be necessary. Certain co-ordinating committees to be appointed by the AIEE standards committee will give this matter due consideration.

References

1. Rating of General-Purpose Induction Motors, P. L. Alger and T. C. Johnson. AIEE Transactions, volume 58, 1939, pages 445–59 (September section).

2. Duty Cycles and Motor Rating, L. E. Hildebrand. AIEE Transactions, volume 58, 1939, pages 478–83 (September section).

Paul H. Rutherford: Mr. Lukens has amplified the statements regarding the application of the motor to a refrigeration compressor on the basis of starting and accelerating torque but confines his comments to integral-horsepower capacitor motors which were not included in the paper. He does not state, however, whether he is referring to 40-degree or 50-degree-rise motors which is necessary when considering any torque comparison, between repulsion-start induction-run motors and capacitor motors. He states that the pull-up torque and starting current are the limiting factors which is not always correct as the starting torque of a capacitor motor is the limiting factor in most applications, particularly in the fractional-horsepower sizes. It is hard to agree with the value of 0.26 pound-feet pull-up torque per locked ampere for this reason.

It is certainly agreed that an inherent heating-overload protector is necessary and advisable on all capacitor-start motors for refrigeration applications but it is not yet fully determined that an automatic device is the best type and preferable to a manual-reset device on all sizes of motors. This will only be determined by experience. Mr. Lukens' statement that high maximum running torque is important does not coincide with his other statements and it will be found that in well-designed motors of either the capacitor or repulsion-start induction-run type there is ample maximum torque when the necessary starting and pull-up torque are obtained for the application.

Mr. Packer confines his comments to hermetically sealed compressors for commercial refrigeration use. It was not intended that the paper should include this type and we therefore, cannot see any direct bearing on Mr. Packer's comments on the paper.

Mr. Packer states that there is nothing to be gained by holding a standardized ratio of starting torque to horsepower. It was the author's point to try to select the horsepower as a ratio to the starting torque per ampere or the accelerating torques. We feel Mr. Packer has tried to apply the statements of the paper to sealed units. The paper did cover only belted units for commercial refrigeration and air-conditioning applications.

It is felt that all those discussing the paper with the exception of Mr. Fuller agree that a motor can be selected for a belted refrigeration compressor on a basis of starting and accelerating torque which Mr. Fuller states is impossible. He does not give any reason for selecting motors on the basis of maximum torque except that from the meager data on the experimental torque-recording device he speaks of, there is some evidence of the maximum torque being important. No specific sizes of compressors are mentioned but from the reference to a four-cylinder compressor it is probable that he is referring to a motor larger than five horsepower. It has been the experience of the author as well as many other refrigeration engineers after examining several thousand motors returned from the field during the last 15 years that failure of these motors was due to their not coming up to speed. This is evidence that these motors did not fail for lack of maximum torque but because of an insufficient starting or accelerating torque.

Mr. Fuller's comments on d-c or polyphase motors may be correct but these were not included in the paper.

Mr. Cain does not say whether the value of 26.9 amperes per horsepower is based on a 40-degree or 50-degree-rise motor. If this value is for a 40-degree motor, it is felt advisable to go to a 50-degree rather than raise the starting current limits as now used by the power companies.

Mr. Hellmund has commented on the intermittent character of the load taken by a refrigeration compressor. This is perhaps true of sealed-in units for household use but not so much with the conventional type unit as described in the paper.

Mr. Veinott's comments on the naming of the refrigeration unit by the horsepower rating of the motor was touched on slightly in the paper. Since current practice is to overload belted refrigeration motors to 50 degrees centigrade rise or more and successfully, it is felt that standards should be made that will acknowledge this fact. It was also suggested that since a refrigeration motor application is a "torque" application motor, torque should be tied up with name-plate horsepower stamping. It is felt that these two points are of utmost importance.

Mr. Veinott feels that the torques should be shown in per cent of full load torque rather than in pounds per horsepower. These figures seemed more convenient at the time but could be converted to percentages or ounce feet by simple arithmetic calculations.

Simplified Precision Resistance-Welder Control

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ASSOCIATE AIEE

Synopsis: The types of resistance-welding applications requiring precision control heretofore characterized by electronic equipment are well-known. Attempts to extend the field of application to borderline operations have not met with complete success because of the initial expense and complicated nature of the control available. A simplified form of precision control consisting of a synchronized magnetic contactor and motor-driven timer overcomes these objections without compromise on results obtained.

ALTHOUGH many new types of spot-weld timers have been announced within the past two or three years, nearly all belong to a group that is characterized by a series of magnetic relays operated in conjunction with one or more time delay units of the electromagnetic, electrostatic, or mechanical types.[1] In each instance a magnetic contactor of considerable size is required to make and break the circuit to the primary of the welder transformer.

Relative simplicity and low initial cost have prompted the use of these nonsynchronous devices for all applications where extreme accuracy is not required. Operating limitations are well known and errors equivalent to *at least* a plus or minus one-half cycle of current are certain to occur although the actual timing period is perfectly metered. These errors result from inconsistent operation of standard a-c magnetic contactors and relays as well as more serious discrepancies introduced by failure to close and open synchronously the power line leading to the welder transformer.[2]

Effect of Nonsynchronous Control

The first specimen in figure 1 illustrates the effect of nonsynchronous control on welding of light-gauge stainless steel. Section *b* is a series of four good welds made with a regular load-current wave form and a one-half-cycle timing period. The welds are strong and the surface of the work is only slightly discolored. The welds in section *c* were made with the same pressure, current, and timing period but a transient wave form was obtained by changing the point on the reference voltage wave at which the welder transformer was energized. All four welds are overheated, testifying to the fact that the character of the wave form does affect the quantity of heat delivered to the weld. Section *a* contains a third group of four welds made on the same piece of work with equivalent current, pressure, and time setting. The power circuit was closed at a point which gave relatively normal wave form but it was opened slightly after zero, allowing nearly a full one-half cycle of arcing at the tips of the magnetic contactor. The overheated welds which resulted are a clear indication of the additional heat delivered to the work when an arc was allowed to occur as the power circuit was broken.

Assuming that an absolutely accurate timing period could be obtained with any one of the nonsynchronous units already mentioned, the variation in results evidenced by the three sections of specimen 1 could be expected. Of course, the effect of these variations is less noticeable when the nature of the work is such that longer timing periods can be used or the fusion point of the material is less critical. However, consistent results using a timing period as short as one-half cycle can be obtained when suitable synchronous control is used. Specimens 2 and 3 of figure 1 illustrate this fact in connection with 24-gauge stainless steel. Notice the consistency of the welds on specimen 2. Their strength is demonstrated by the "slug" of metal which has been pulled from specimen 3.

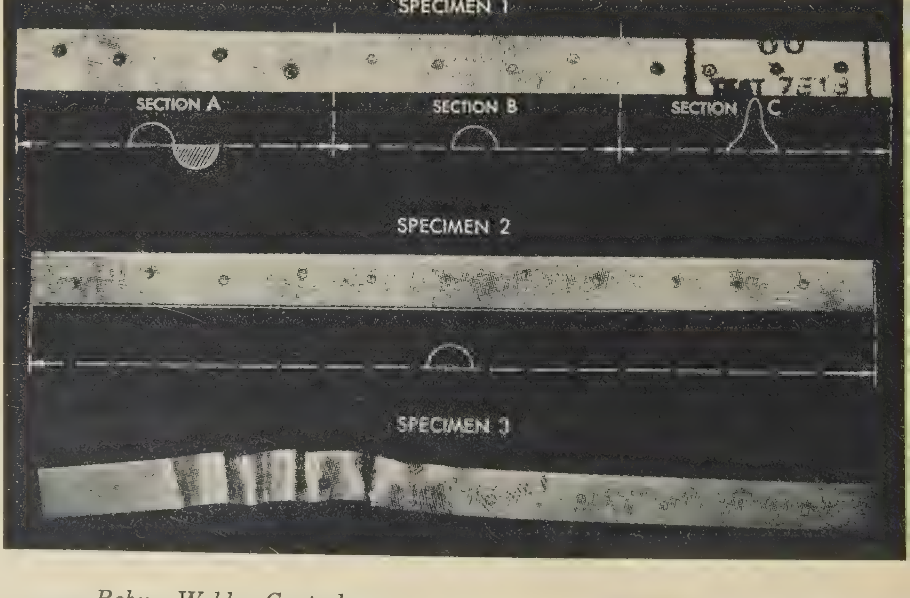
Previous Synchronous Equipment

At least three general types of synchronous control equipment have been proposed to overcome the variations just described. The first of these—full electronic control using mercury-pool-cathode-type power tubes instead of magnetic contactors—has served its purpose well.[3,4] The chief objections are high initial cost as well as complicated operational and maintenance problems.

Synchronous-motor-driven contactors have also been offered as a solution.[5] Although reasonably priced, their adjustment is critical and difficult to maintain.

A more recent development is the so-called "impulse contactor."[6] Energy stored in a capacitor is discharged into the operating coil of a magnetic contactor causing its contacts to close and open at points determined by the inertia of the moving parts as well as the character of the contactor finger spring and

Figure 1. Stainless-steel specimens showing effect on weld of nonsynchronous control compared with consistent results obtainable when suitable control is used



Paper number 39-65, recommended by the AIEE committee on electric welding, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 14, 1938; made available for preprinting December 29, 1938.

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1. For all numbered references, see list at end of paper.

the quantity of energy delivered to the operating coil. The equipment required involves considerable expense and the operating range is definitely limited. Proper adjustment is sometimes difficult because of the several related factors which influence the final setting.

Simplified Precision Control

This paper deals with a form of precision control unit consisting of a synchronous magnetic contactor and motor-driven timer. Two new developments make the system possible. The first is a special magnetic contactor consisting of two separately actuated poles connected in series (figure 2). One pole is closed early in the operating cycle. The closing of the second pole energizes the power circuit and opening the first de-energizes it. The actual period during which the transformer is energized is measured by the interval during which both poles are closed. Operating speed of the individual poles is not a limiting factor. Timing periods as short as one-half cycle are easily obtained.

The system just described is old but construction features of the contactors used are new. The closing contactor is adjusted to take advantage of a little-known operating characteristic. When properly designed a contactor's magnet coil can be energized at any point within a rather wide band and yet the tips of that contactor will close at essentially the same point on the reference voltage wave. The speed at which the contactor closes is influenced to a considerable degree by the amount of transient in the operating coil circuit. When correctly adjusted the energizing of that

Figure 2. Precision contactor consisting of closing and opening poles operated in sequence. Synchronizing equipment for opening pole is mounted on right-hand side of panel

circuit earlier than normal results in a decreased transient and a slower closing speed. Thus, the contact tips touch at a point equivalent to that of a much later energization of the coil circuit. Likewise, the increased transient which results from late energization of the coil compensates for the delayed start in that the contactor closes faster. This characteristic can be employed to insure consistent closing of the power circuit, thereby avoiding various degrees of tran-

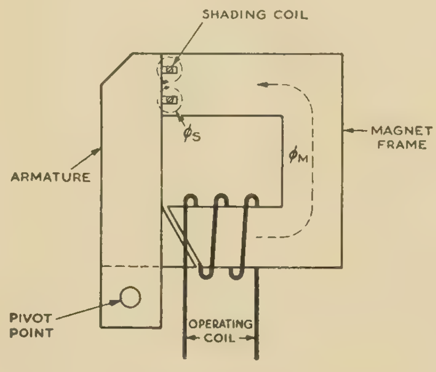


Figure 3. Sketch of magnet frame showing typical shading-coil circuit

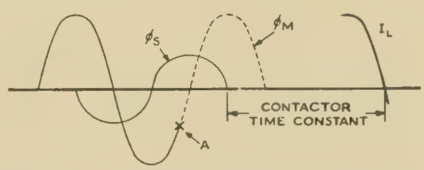


Figure 4. Phase relationship of main (ϕ_M) and shading-coil (ϕ_S) fluxes

sient and resulting variations in heat delivered to the work.

The opening contactor is self-synchronized to insure the parting of contact tips at the minimum arcing point. Because of the nature of the device there is no need for synchronous interruption of the contactor operating-coil circuit. Regardless of the time at which the circuit is broken the contactor opens at the point for which it is set.

Synchronous Magnetic Contactor

Synchronization to within a plus or minus ten electrical degrees or better is rather easily obtained by separately energizing the shading-coil circuit of the contactor. All a-c contactor magnets are equipped with a shading coil to insure quiet operation. The conventional design (figure 3) consists of a short-circuited turn of some conductive material encircling about two-thirds of the magnet pole face. Flux (ϕ_M) set up by the operating coil generates a voltage in the shading-coil circuit and the resulting current provides a flux (ϕ_S) which is out of phase with the parent flux (figure 4). If this supplementary flux did not exist the magnet surfaces of the contactor would part momentarily at each period

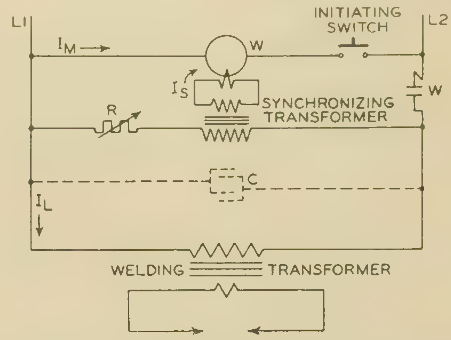


Figure 5. Schematic diagram of synchronized contactor circuit

during which the main flux passes through zero and an objectionable vibration would result.

By separately energizing the shading-coil circuit, synchronized opening of the contactor can be obtained without sacrificing quiet operation. Refer to the schematic diagram shown in figure 5. When the push button is closed the operating coil of contactor *W* is energized. The closing of the contactor energizes the welding transformer and simultaneously applies power to the primary of the small transformer which separately energizes the shading-coil circuit. Because of the difference in power factor between the operating-coil and shading-coil currents, the two out-of-phase fluxes necessary to quiet operation are obtained. Assume that the push button is released at the point marked *A* in figure 4. The main flux (ϕ_M) goes to zero but the shading coil flux (ϕ_S) is sufficient to keep the magnet surfaces sealed until it approaches zero value. Because the main flux is not there to supplement it, the magnet surfaces part. Although the shading-coil flux resumes in the negative direction before the armature of the contactor has moved enough to part the contact tips the contactor continues to open because the shading coil alone is not strong enough to reclose it.

The separately energized shading coil insures that the magnet surfaces always part at a point equivalent to the zero value of the shading-coil flux but the armature of the contactor must move an appreciable distance before the contactor tips part. The interval required for the movement is called the "time constant" of the contactor. Since the zero point of the load current bears no relation to the shading-coil flux the "time con-

stant" of the contactor must be equal to the difference in time between a zero point in the shading-coil flux and a later minimum value in load current. Adjustment can be made by changing wear allowance (sometimes called "follow-up") or spring tension. However, a more convenient method is available. A rheostat in series with the primary of the synchronizing transformer varies the voltage applied to it. The corresponding change in magnetizing current drawn by the transformer primary winding provides a variation in power factor of the shading-coil current. Since a phase shift of about 60 electrical degrees is practical with this method the "time constant" of the contactor can remain the same while the phase relationship of the shading-coil circuit may be changed to locate the minimum arcing point. Once adjusted, the contact tips part at the selected point independent of the time at which the operating-coil circuit is broken.

Many attempts to synchronize the opening of a power circuit in conjunction with magnetic or motor-driven contactors have failed because of a tendency for the arc to restrike after it presumably had been extinguished. The oscillograph trace represented in figure 6 indicates the voltage drop across the contactor tips. When they are closed only the reference line represented by the sweep of the oscillograph is recorded. As the tips part an arc is drawn and the drop across the arc is recorded in the form of a small voltage in phase with line current. When the load current passes through zero the arc voltage must also go to zero. Since the contact tips have actually parted prior to this time the oscillograph must instantly record line voltage across the gap. Such is the case in test *A*.

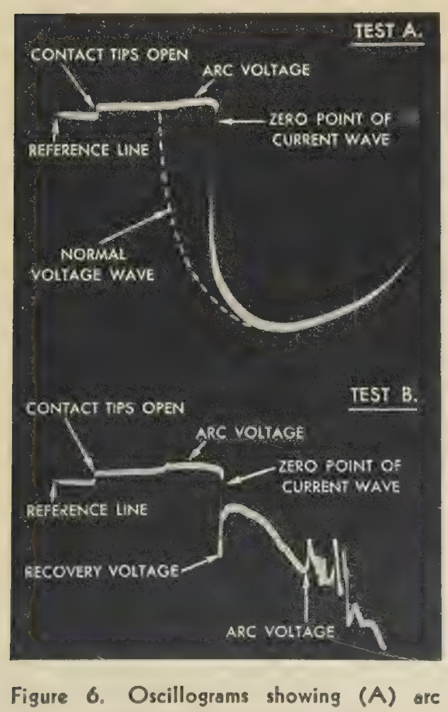


Figure 6. Oscillograms showing (A) arc interrupted, (B) arc restriking

For test *B* the contactor tips are parted at a point equivalent to that represented in test *A*. A similar arc voltage is recorded. It is reduced to zero with the load current. The voltage across the contact gap instantly approaches line voltage but the dielectric strength between the contact tips has not built up rapidly enough to resist breakdown by the rising voltage. The arc is restruck and maintains until another zero point of the load current is reached. It is apparent that merely opening the circuit slightly before the current zero is not enough to eliminate arcing.

Two solutions to this problem are available. The rate of voltage recovery can be retarded by means of resistance or capacitance connected in parallel with the load.[7] This delay gives the dielectric an opportunity to build up to a safe value. A second course of action is to alloy the contact tip material with elements intended to hasten the building up of the gap dielectric strength.[8] Mercury, because of its low vaporizing point, is a logical choice but it cannot be alloyed successfully with copper. However, cadmium, another choice of material with good deionizing properties, can be obtained in copper alloy form at reasonable cost. A combination of both corrective factors has proved to be the practical answer to the problem.

The synchronized magnetic contactor is an important development in connection with simplified precision control but it has an even broader field of application as an improved magnetic contactor for use with all kinds of welding machines involving heavy loads and frequent operation. The elimination of arcing at the contact tips overcomes the most pronounced limitation in contactor application. Mechanical life is above reproach but until now electrical ratings have been determined primarily by the heat liberated during the arcing period, and contact-tip life has been reduced proportionately. Figure 7 illustrates a 300-ampere (nominally rated) single-pole synchronized unit which has been successfully tested on a frequent operating cycle at 1,800 amperes, 220 volts, 47 per cent power factor.

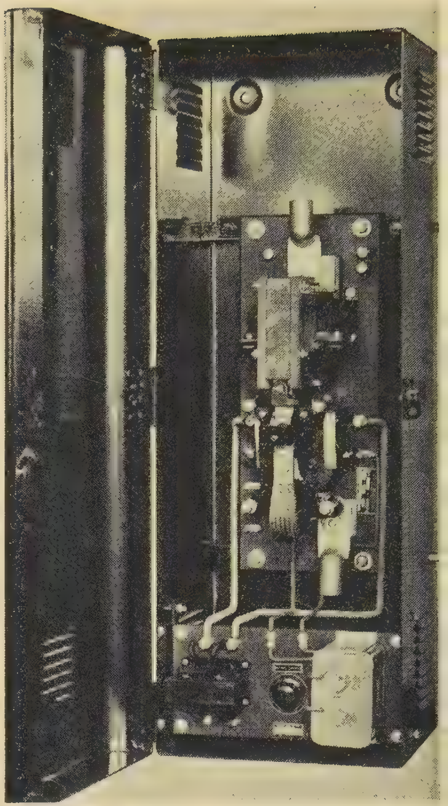


Figure 7. Single-pole 300-ampere (nominally rated) synchronized contactor for general application

Motor-Driven Timer

The second development contributing to the simplified precision control is a new form of synchronous-motor-driven timer. Devices of this kind in the past have involved one of two distinct design problems. When a continuously operating motor was used, a magnetically operated mechanical clutch was required to begin and end the welding operation. Positive synchronization could not be maintained through such a mechanical linkage. When an attempt was made to start and stop the motor at the beginning and end of each operation, poor synchronization resulted because of the tendency to "hunt" on starting and to coast in stopping.

The motor of the timing device under discussion is allowed to run continuously but a clutch is not required. The schematic circuit diagram shown as figure 8 illustrates this fact. Although the pilot contact can be closed at any point, the cycle of operation is not begun until the cam actuated contact TR_1 is momentarily made, energizing synchronized contactor W_1 and control relay CR_1. Since TR_1 closes at only one point in the operating cycle, all timing intervals are measured from that point. Relay CR_1 forms a holding circuit about TR_1. Additional rotation of the cam shaft closes contact TR_2 which energizes contactor W_2 and completes the circuit to the welder transformer. Relay CR_2 is

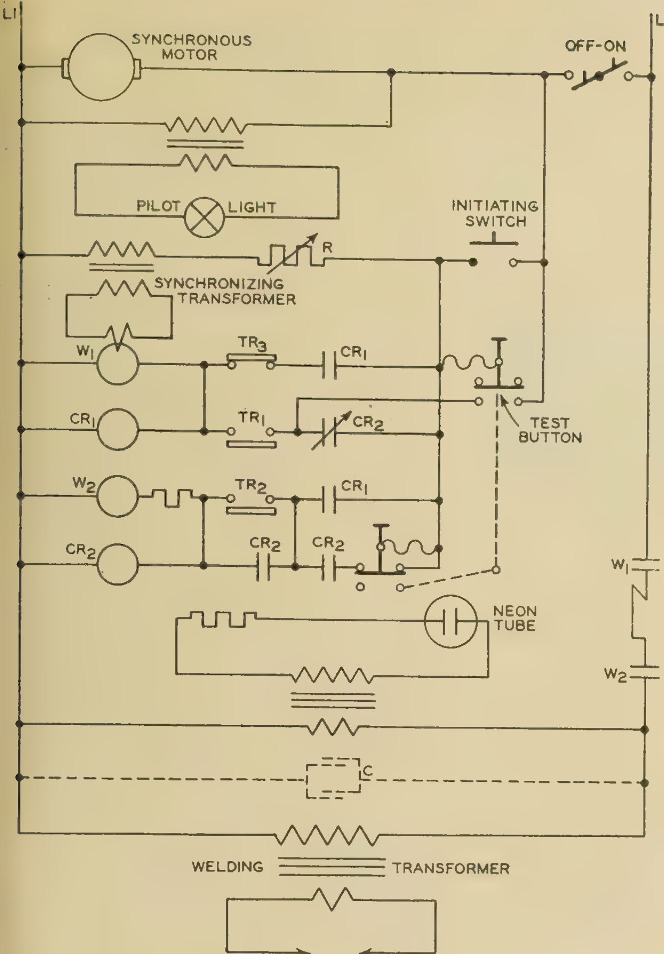


Figure 8. Schematic diagram of motor-driven timer circuit

closed at the same time to form a holding circuit around TR_2. Final rotation of the cam shaft opens contact TR_3 in the circuit to the operating coils of contactor W_1 and relay CR_1. The synchronized welder contactor then opens at the minimum arcing point. Relay CR_2 and contactor W_2 remain closed until the initiating switch is released. A normally closed contact on relay CR_2 prevents unauthorized repeat operation.

The weld period adjustment is made by turning knob C in figure 9. Its movement is transmitted through a pinion to a large gear on which contact TR_2 is mounted. The angular position of contact TR_2 with respect to the motor cam shaft determines the point at which welding contactor W_2 is closed. A pointer actuated by movement of the gear indicates the welding time selected.

Contacts TR_1 and TR_3 are mounted on steel plates which can be rotated manually to permit initial adjustment. Ball bearings are used throughout to insure precise operation for the entire mechanical life of the device.

Figure 10 is a front view of the timer. A specially designed neon tube energized from a closely coupled transformer connected in parallel with the load indicates the exact welding time and assists in selecting the proper operating points for the magnetic contactors. During the period the welding transformer is energized, the bright glow of the tube is visible through a slot in the scanning disk which is attached to the end of the motor cam shaft. The resulting streak of light A includes dark bands as the supply line voltage passes through zero. Therefore, each section of the trace represents one-half cycle. Counting of the sections is not necessary since the timer dial is directly calibrated in cycles.

A test button B is provided to make possible repeat operation of the control equipment at a rate determined by motor speed. By closely observing the beginning of the light trace on dial as knob C is rotated, the stable operating point of the "closing" contactor can be selected prior to placing the equipment in operation. Proper adjustment will result in a consistent beginning of each successive trace.

Rheostat D is connected into the "opening" contactor synchronizing circuit and controls the point at which the contactor tips open. If the electrodes

Figure 9. Timer with interior removed to show mechanical construction

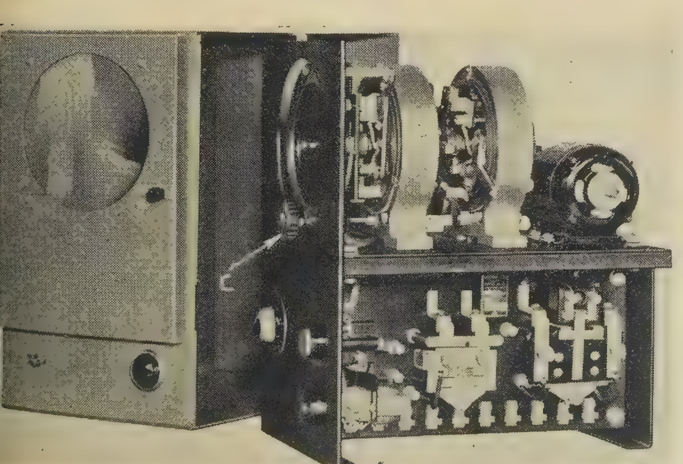
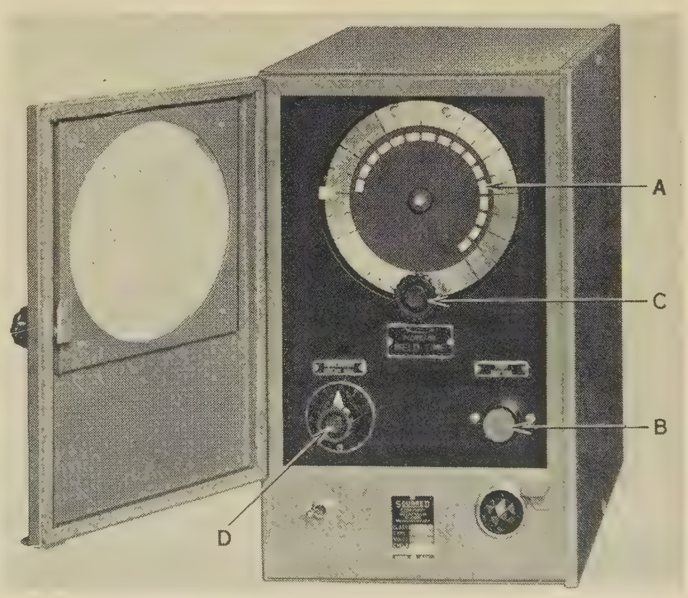


Figure 10. Front view of motor-driven timer showing adjusting means and neon light trace on revolving disk



of the machine are closed before the test button is operated the rheostat can be adjusted for minimum arcing before the welding operation is begun. Arcing will be indicated by an additional half-cycle band on the end of trace *A*.

Additional motor-operated contacts and a slight rearrangement of the circuit

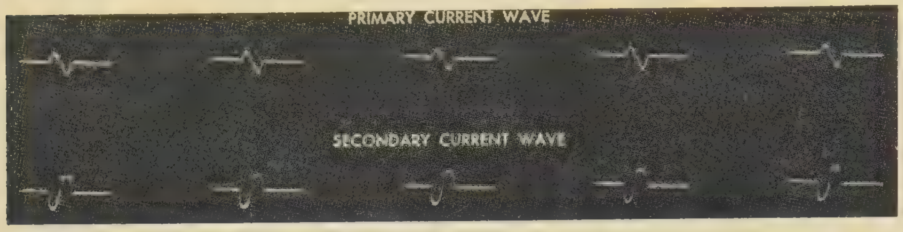


Figure 11. Oscillogram showing accuracy of precision control equipment breaking a load equivalent to 300 per cent of contactor nominal rating

would permit the control of all four timing intervals (squeeze, weld, hold, and off periods) required in connection with fully automatic air-operated welding machines. Three knobs on the front of the panel would permit individual adjustment of all periods. The number of operations per minute is determined by the speed of the motor-driven cam shaft and could be varied by changing reduction gears between motor and cam shaft, by means of a simple transmission operated from the front of the panel. A dial calibrated in percentages and referred to the total number of cycles equal to a single revolution of the cam shaft at the prevailing speed would provide at a glance the relationship to each other of the component parts of the operating cycle.

The equipment just described is relatively inexpensive and easy to operate. Its mechanical construction is such that continued service without attention can be expected. One installation studied for a period of more than eight months was attended only by an untrained operator. Adjustments were readily made and no change was required until the end of the production run.

The accuracy obtainable with the simplified equipment is illustrated in figure 11. A cathode-ray oscillograph screen recording, in turn, the primary and secondary current wave forms of a welding transformer drawing about 300 amperes on the primary side was photographed at 30-second intervals during an 18-minute test. Throughout the entire operating period the circuit was established 150 times per minute for a duration of one cycle. The individual photographs apply in order to observations spaced 75 operations apart. Wave form and accurate timing testify to the consistent operation obtained. These features of the control equipment are responsible for the performance indicated:

1. Special magnetic contactor consisting of two separately actuated poles operated in sequence and having

(*a*). The circuit-closing pole adjusted to take advantage of natural response characteristic of the contactor.

(*b*). The circuit-opening pole self-synchronized to open at minimum arcing point independent of timer setting.

2. Motor-driven timer with a continuous-running motor but without a mechanical clutch.

3. Means for selecting the stable operating points of the magnetic contactors as well as determining the actual welding time and consistency.

Conclusion

It is reasonable to believe that many welding operations made on machines not now equipped with precision control could be made much less expensive if

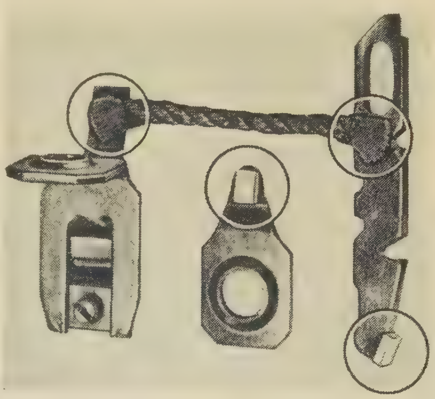


Figure 12. An assembly of small parts involving copper to copper, copper to bimetal, and copper to silver-alloy joints is successfully welded in production using simplified precision control

proper control were installed. Likewise, many parts previously fabricated by other means could now be welded. Only the high initial cost and relative complexity of the control heretofore available has prevented such action. F example, a company manufacturing sma circuit-breaker parts found it necessary fasten contacts composed of silver allo material to bronze connectors (figure 12 The same assembly included copper copper and copper to bimetal joint A motor-operated welding machine equi ped with one of the better nonsynchr nous control units was used. Because the critical nature of the operation a the rigid inspection requirements of t assembly, rejections were estimated 23 per cent. After the installation simplified synchronous control equi ment the rejects were less than one p cent and those were attributable to caus other than welding.

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Discussion

L. G. Levoy and **G. W. Garman** (Gener Electric Company, Schenectady, N. Y. Mr. Roby has given an interesting pap and has described an ingenious method improving the accuracy of contactors applied to resistance spot-welding machine This improvement in accuracy should r sult in better welding from contactor-co trolled machines and will expand the fie of contactor control. The impression given that this contactor is equivalent price and accuracy to precision electron control and that it is less complicate Electronic control without question more accurate and in most applicatio does not require any adjustments excep for the occasional replacement of a tub which can be quickly and easily change While it is true that some electronic co trols are relatively more complicated, it only because the demand has been such to require the flexibility and accura which they provide.

With reference to these more complicate control circuits, it has been interesting observe the trend in the various require ments with increased use and confiden in this type of control. Several years ag it seemed that the time had arrived f

simplification, but to our surprise accurate and consistent welding had led to the desire of welding methods and processes hitherto believed impossible or too expensive. This trend has led to the development of the interrupted type of welding and to the increased use of "heat control" by the phase-shifting method. In addition, there is a strong tendency toward the use of complicated time and heat control cycles. Thus the design engineer is faced with two conflicting trends, one toward simplification in an effort to reduce costs, which has led to the development of the ignitron contactor, and second, toward more flexible controls which inherently leads to higher costs.

The author states that the contactor tips open consistently within ten electrical degrees, but does not give specifically the accuracy of closing. It would appear that there are several factors which would affect the closing time and the opening time. It is expected that a reduction in line voltage would not only decrease the welding current in the normal manner but would retard the closing of the contactor, thereby reducing still further the welding current, particularly for short welding periods. Also, since the mechanical time constant of the contactor is fixed and does not necessarily agree with the power factor of the welder, a starting current transient may be present in the welding circuit. This transient is not necessarily detrimental, but will decay exponentially, producing variations in the current zero for different time settings. It is also well known that while the thickness of the material being welded does not affect appreciably the secondary current in low-power-factor machines, it does affect the power factor of the primary current. We would therefore like to ask Mr. Roby how critical is the adjustment of the shading-coil rheostat and what is the effect of these variations on this setting.

Variations in the phase angle of closure of the contactor tips with respect to the alternating voltage wave materially affect the root-mean-square current and therefore the heat delivered to the weld. The heat delivered to the weld is directly proportional to the resistance of the work, the time of application of the current, and to the square of the root-mean-square current. The magnitude of the variations in heat caused by small angular changes in the phase of closure can best be shown by a typical example as given in table I of this discussion. These results apply to a welder having a 0.30 power factor.

In this table, the angle of closure is measured from the power-factor angle. Positive angles are later in time corresponding to closure $^1/_{2,160}$ second too late on the wave. Negative angles correspond to closure too early by the same amount. In each case it is assumed that the contactor opens precisely at the current zero, so that the variations shown are entirely due to erratic closure of plus or minus ten electrical degrees. The heat delivered resulting from closure at the power-factor angle is taken as 100 per cent as a point of reference for each case. These variations, which amount to more than 50 per cent in the heat for a one-half-cycle spot length and 16 per cent for one-cycle spot length, are too large to be tolerated in many applications. In view of these variations, we would like to ask the author what precision, as regards phase angle of closure, can be maintained in practice with this device.

Furthermore, if the timing is adjusted for an odd number of half cycles, the resulting residual flux in the welding transformer may cause excessive sparking when the welding electrodes are lifted unless the shunt capacitor, which is connected across the primary of the welding transformer, can absorb this energy or unless antipolar starting be used.

Table I

Spot Length	Closure Angle, Measured From Power Factor (Degrees)	Per Cent Heat
One-half cycle	+10	73.1
One-half cycle	0	100
One-half cycle	−10	128
One cycle	+10	92.5
One cycle	0	100
One cycle	−10	109

The oscillograms shown in figure 11 show a small discontinuity in the primary current wave such as might occur if the contactor bounced and arced, or started to drop out. Will the author please explain this discontinuity? Unfortunately, the scale of these oscillograms is so small that variations of current of a magnitude which might give unsatisfactory welding would be imperceptible.

One disadvantage of the motor-driven timer is that there is a variable time delay after the closure of the initiating switch before TR_1 closes, starting the cyclic process. The time delay depends upon the phase of closure of the initiating switch with respect to the position of the cam actuating TR_1. It may therefore be as great as one complete cycle of operation, or in other words the time delay may be as great as the time required to make a spot weld. In high speed production, allowance would have to be made for this maximum time delay, which may in some cases limit the rate of production. Electronic spot-welding control eliminates this variable time delay. In addition, electronic control permits the use of phase control, giving a smooth variation in heat without resorting to taps on the welding transformer. In many applications phase control has proved to be a most desirable feature. Contactor control eliminates the possibility of using phase control.

Because of its relative inflexibility and inaccuracy, this control is limited to certain spot-welding applications. Good seam welding requires more precise timing such as is obtained electronically because of the possibility of cumulative transients during the seam, so that the error may be much greater than the error of any single spot. Furthermore, because of life considerations, a contactor would not give adequate life on most production seam-welding jobs. Repeat operation is limited to the number of gear changes available. A recent control has been developed which provides the equivalent of at least 60 gear changes on this unit and maintains electronic accuracy. The cost of incorporating this large number of gears to make the device equivalent in flexibility with this new electronic control would probably be prohibitive. On many production jobs this great flexibility is not required. However, frequently production jobs change and it is obviously an advantage if the control is adaptable to any such possible change.

In closing, we would like to ask the author to present some actual life figures in terms of the number of operations possible. Operation in terms of time is of little value unless rate is also specified. Electronic controls have been in use for many years and the number of operations possible without changing tubes soars into astronomical proportions—hundreds of millions.

F. H. Roby: A discussion of the paper, "Simplified Precision Resistance Welder Control," prepared by D. G. Levoy and G. W. Garman, is typical of an attitude that has prevailed whenever magnetic contactors are mentioned with reference to accurate or precision control of a resistance weld. The author has no quarrel with electronic equipment. Such devices have, without question, performed to specification. Nevertheless, it is a fact that the least expensive standard electronic control costs more than many users are willing or able to pay. The result has been the continued use of nonsynchronous devices for applications where their inherent errors are objectionable. Since a device of the type described in technical paper 39–65 can be manufactured and sold for a price much less than that of the equipment now available, precision control can now be applied to more jobs.

It is true that an electronic tube is a simple switching device when judged by an electrical engineer. However, the typical electrician, maintenance man, or operator

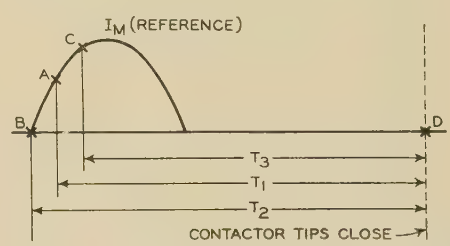

Figure 1

finds any electronic device something of a problem. When not understood, the very characteristics which seem simple to the engineer result in complications to the individual charged with using and maintaining the equipment. Because he is usually acquainted with mechanical motions and magnetic forces, a system composed of several such parts is readily understood and, therefore, simple. It will require many years of instruction to dispel this attitude.

Precision welding cannot be satisfactorily accomplished in the face of widely fluctuating line voltages. It has been stated by experienced users that ten per cent variation may seriously affect results. Standard contactors are equipped with magnet

coils designed to operate at 15 per cent under- and 10 per cent overvoltage. Welding contactors are equipped with intermittent duty coils that are even less affected by undervoltage.

Experience has indicated that a contactor can be closed more consistently than opened because of the stronger forces and faster accelerating rates involved. Using this experience as a basis, early investigators provided one normally open and one normally closed contactor operated in sequence respectively to close and open the circuit. Thus both functions were accomplished by energizing a magnet.

Accuracy in closing is improved considerably by utilizing the "flat operating characteristic" of a magnetic contactor. As pointed out in the paper, the closing speed of a contactor is influenced to a considerable degree by the transient drawn in its operating coil. Assuming a normal wave form, the pull of the magnet is proportional to the product of the pole face area and the *flux density squared*. Under transient conditions the flux in the iron circuit may be as much as *double* normal value. Since the area of the pole face remains constant, the pull could be as much as four times normal. It is not difficult to believe that the closing speed of the contactor is changed accordingly.

Refer to figure 1 of this discussion for a graphic explanation of the "flat operating characteristic." Assume that the magnet is energized at point A on the coil current wave. A transient wave form will be obtained and the magnet pull which results will cause the contact tips to touch at point D. Time T_1 is consumed in closing. However, if the coil circuit is energized at point B no transient exists and the lessened pull causes the contactor to require T_2 time for closing thereby compensating for the early coil energization. Likewise, energizing the coil circuit at point T provides a greater transient and increased pull. Closing time T_3 results, thus compensating for the late energization in the magnet circuit. Proper adjustment of the closing contactor is obtained when its magnet is energized at point A (the middle of a 60-electrical-degree band). The neon tube indicator assists in obtaining the correct setting.

Under perfect operating conditions there will be small variations in the closing point. However, large-scale oscillograph records indicate that these are not enough to affect results in most instances. The oscillogram reproduced as figure 11 is not enlarged enough to serve this purpose but it does indicate a consistent wave form which, in turn, is the result of consistent operation. The small "break" in the oscillograph trace is caused by contact tip bounce in closing. This bounce is the result of energy stored in the contact tip and is characteristic of all magnetic contactors.

The tips of the opening contactor are usually set to part at a point approximately 30 electrical degrees before current zero. Because the current value is rapidly approaching zero, no appreciable arcing can be noticed. Assuming a $\pm$15-electrical-degree error under the least favorable operating conditions, the power factor of the load would be required to increase from 50 per cent to 80 per cent to cause arcing. A decrease in power factor would have no effect other than to cause the tips to open slightly higher on the current wave, thus drawing a little more arc. The adjustment is not critical.

An odd one-half cycle is included in the time required for the timer cam shaft to make one revolution thus giving the effect of antipolar starting.

The elimination of a mechanical clutch in the timer system through the use of an "initiating relay" has introduced a variable time delay in starting the weld. This delay is objectionable only when extremely high production rates are involved. Since most precision jobs are necessarily of low production because of care in mechanical alignment and frequent dressing of electrodes, this objection is not a valid one. It would be possible to employ a clutch in the system without materially affecting the accuracy of the timer but the advantage gained does not warrant the introduction of an additional mechanical link.

The authors of the discussion are unde the mistaken impression that mechanica equipment is to be recommended for sear welding. A satisfactory seam weld ma require from 10 to 16 interruptions per incl Assuming a feed rate of 90 inches per mir ute, the equipment would be required t operate at the rate of from 900 to 1,440 com plete cycles per minute. Mechanical cor tactors should be limited to about 40 operations per minute since their mechani cal life is determined somewhat by th speed at which they operate. Thus sear welding is not considered a suitable field c application.

Heat control is of value on some applica tions where extremely fine increments c adjustment are desired. The many ap plications falling outside of this group repre sent a field broad enough to justify th development of a precision controller whic does not include the heat-control feature.

Magnetic contactors designed for welde service have long since been perfected to th extent that service in excess of 15,000,00 operations is obtainable without prohibitiv maintenance. Under severe operating con ditions automobile plants use the same equip ment for several programs. In lower-pro duction shops the contactors will serv indefinitely. It is only natural that con cerns recently associated with electroni devices are not acquainted with curren contactor performance data.

A fully automatic welder control unit i practical without considering 60 gea changes. Flexibility of the type mentione would probably be necessary in connectio with a laboratory installation but norma production facilities would involve only si or eight.

Experience over the past 18 months ha demonstrated that many difficult weldin jobs can be readily accomplished wit the equipment under discussion. It i not intended that it be used to the exclu sion of all other types of control. Insteac it should be considered as a supplement t control now available, bringing into reac of the average user the advantages of prec sion control.

Load Ratings of Cable

HERMAN HALPERIN
MEMBER AIEE

Synopsis: Operating and test data concerning the maximum safe loading of impregnated-paper-insulated lead-covered cable are presented. The results of the study may be summarized as follows:

1. The occasional operation of cable at higher temperatures than are permitted by present temperature rules effects considerable economy.

2. During emergencies, temperatures of 5 to 35 degrees centigrade (depending on kind of cable) above those permitted by the rules are safe for the insulation.

3. For extra-high-voltage solid-type cable, void formation in insulation and expansion of lead sheaths may limit allowable temperatures and temperature ranges.

4. Cracking of lead sheaths due to reciprocating cable movement into manholes may limit the temperature range for usual daily loading. Limitation is more severe for longer conduit lengths up to 500 feet, but changes little with increase from 500- to 1,000-foot lengths.

5. Cracking of sheaths in manholes due to cable movement may be reduced by improving manhole conditions.

6. For many cables a balanced design requires a lead-alloy sheath that gives increased resistance to effects of cable movement and of internal pressures.

7. Continuous field temperature surveys are essential to efficient use of large conduit and cable systems.

8. Only a small fraction of the cable ever operates at the higher temperatures.

9. Data on center empty-duct temperatures and on average heat losses over 24-hour periods give satisfactory results in heat calculations.

10. Other practices which increase load ratings are the use of different ratings for various periods of the year, the replacement of poor soil in special cases, and the use of extra-large conductors in warmer conduits.

THE PURPOSE of the investigations covered in this paper has been to obtain the most efficient use of impregnated-paper-insulated lead-covered cable and accompanying conduits and manholes. Using considerable recent data, the author has attempted to cover in one study all important factors affecting load ratings.

With the sharp improvements during the past 20 years in the quality of cable and joints and in their installation and maintenance, there has been an accompanying sharp decrease in the rate of failures; for example, during the past 15 years in Chicago the total rate of failures of 12-kv lines consisting of 500,000-circular-mil three-conductor 13-kv belted cable has decreased from about 40 failures to 5 failures per hundred miles per year. At the same time, the quality of terminal equipment has improved greatly. These trends show a sharp decrease in the frequency with which low- or high-voltage circuits are being subjected to loads approaching their maximum ratings on account of outages of parallel circuits.

The outgrowth of these improvements, together with new knowledge on the effects of high temperatures on cable and joints, has been the definite establishment during 1930–37 of emergency load ratings for cable in Chicago operating from 120 volts to 132,000 volts. Generally emergency ratings determine, with the exception of the 120-volt a-c network cables, when additional underground circuits must be installed.

If, for example, the maximum rating of 500,000-circular-mil three-conductor 13-kv cable is increased by 15 per cent by allowing temperatures to exceed occasionally the maximum given in AIEE standards,[1] then on a growing system large reductions in the immediate and future investments needed for additional circuits and conduit will be obtained. With the increases in copper losses, in dielectric losses, and in maintenance costs at the higher loadings taken into account, the net reduction in total annual investment and operating charges per kilovolt-ampere mile for installed cable and conduit would be 9 per cent, assuming that the higher temperatures did not cause the cable life to be less than would be required from the standpoint of obsolescence and system changes. The increased ratings might be obtained on future lines without exceeding the maximum temperature given in the standards by using 650,000- instead of 500,000-circular-mil cable, but the total annual charges per kilovolt-ampere mile would then be reduced only one per cent.

On account of the use of items having standard ratings, terminal equipment frequently can safely carry more load than the cable connected to it. Increased load ratings of cable, therefore, produce substantial reductions in the cost per kilovolt-ampere carried through such equipment. An important overall advantage is obtained by higher loading in reducing the space required in the streets for conduits and at stations and substations for terminal facilities.

Various aspects of the problem of load ratings are discussed in four divisions of the paper:

Part I—Limitations due to the insulation

Part II—Limitations due to the sheath

Part III—Heating characteristics of conduits

Part IV—Principles and methods of calculation

Part I. Limitations Due to the Insulation

Since 1925 the maximum allowable operating temperature for solid-type impregnated-paper insulation has been 90 degrees centigrade minus the rated voltage in kilovolts, with a maximum of 85 and a minimum of 60 degrees centigrade. For multiple-conductor belted cable the rated voltage between conductors, and for shielded and single-conductor cable the voltage between conductor and sheath is used. This rule was apparently based on the following: above 85 degrees the rate of deterioration in mechanical strength of the paper increases rapidly with increasing temperature; as the operating voltage increases, the stress, and hence the probability of serious ionization, increases.

When this AIEE temperature rule was first established (then it was 85 degrees minus the rated voltage), ionization had not been recognized, and means to measure it were not available. At that time the reason for the decrease in temperature with increase in voltage was that the dielectric losses for high-voltage cables increased rapidly to very high values with temperature and increased the possibility of cumulative heating. For over 12 years, impregnating compounds which give small dielectric losses have been in use.

Paper number 39-29, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 22, 1938; made available for preprinting December 15, 1938.

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The author is very grateful for the assistance received in the preparation of this paper and wishes to make the following specific acknowledgments: part I, H. A. Adler, C. E. Betzer, and O. B. Turbyfill; part II, M. C. Hedges, H. J. Plumley, and F. O. Wollaston; parts III and IV, C. A. Bauer and D. D. Nagley.

1. For all numbered references, see list at end of paper.

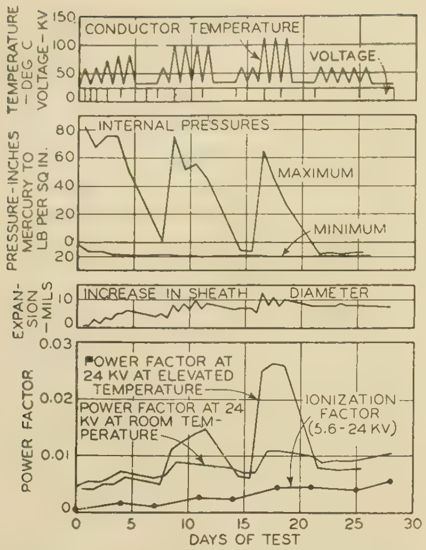


Figure 1. Aging test of sample F (three-conductor 13-kv cable)

The rate of deterioration of paper at various temperatures has been investigated by Massachusetts Institute of Technology,[2,3,4] H. W. Fisher and R. W. Atkinson,[5] F. M. Clark,[6] and others in individual tests varying from ten days to 17 months. The results are in agreement in showing that heat above 85 degrees centigrade causes an appreciable decrease in tensile and tearing strength of paper, but that the dielectric strength is not influenced much until the mechanical strengths have been almost destroyed. The main consideration, therefore, is the influence of heating on the ability of the cable to withstand removal and reinstallation without material lowering of the dielectric strength of the cable.

Experience, supplemented by careful examinations, has shown that the mechanical properties of the insulation of cable that has been in service in Chicago up to 40 years are sufficient to permit withdrawal of the cable and its successful operation after reinstallation, except in the few cases where the maximum temperatures have substantially exceeded the limits given herein for emergency operation. The cable, however, is gradually scrapped before the useful life of the insulation has expired due to (1) refitting to other locations on the system, (2) elimination of sheath damages, and finally (3) obsolescence. Serious deterioration due to ionization has been found mainly in poor high-voltage solid-type cable made 12 or 15 years ago; this has been due mainly to factors other than operating temperatures. Evidently the maximum use of the cable was usually not being obtained, because the loading was insufficient.

The temperature rule makes no provision for increased loads in emergencies for periods of one or two days. Therefore, sufficient carrying capacity needs, under the rule, to be installed so that in the worst expected emergencies the hottest part of the cable in the circuits remaining in service will not exceed the prescribed temperature. Obviously, then, almost all of the cable will be operating far below the temperature limits for the great majority of the time. For example, for the 1,030 miles of three-conductor cable operating at 12 kv in Chicago in 1937, 95 per cent operated up to a maximum of 60 degrees or less, 4 per cent up to 70, and the very maximum was 82 degrees centigrade or only 5 degrees above the temperature given by the rule. For each length of cable, the average temperature for the year was usually 15 or 20 degrees less than the annual maximum. Due to the new emergency ratings, it is expected that in the future the temperatures will range t higher values.

It is, therefore, economical to tak some chances of exceeding the limits i AIEE standards for a few hours or s during an occasional emergency in orde to utilize efficiently the entire systen with the understanding that it migh result in shortening seriously the life c the insulation of one or two per cent of th cable.

Tests[4] have shown that the wood-pu paper available for cable insulation 1926 deteriorated in mechanical strengt less than about five and ten per cent i 50 hours at, respectively, 120 and 14 degrees centigrade and showed no chang in electrical strength. Tests[5] reporte in 1921 showed that Manila papers in us at that time deteriorated with heatin only slightly more than those tested i 1926 and much less than those tested i 1905.

Wood-pulp paper, which began to re place Manila paper about 1924, is use almost entirely now. Marked improve ments have been made in it for electrica insulation since 1926. The tearing an dielectric strengths increased by abou 50 per cent. In addition, improvement have been made in the application of th paper. Thus, paper in recently-mad cables may be permitted much greate percentages of depreciation due to heat ing than paper in earlier cable before th cable becomes unfit for reinstallation The available information indicates tha recently-made paper may be safely op erated about 15 degrees centigrade highe than 1920 paper and about 30 degree higher than 1905 paper.

In general, the subsequent discussio on maximum allowable temperature refer to the temperature for the cable in th

Table I. Tests on 500,000-Circular-Mil Three-Conductor Belted 13-Kv Cable With Pressure-Tight Potheads

Sample	*A*	*B*	*C*	*D*	*E*	*F*	*G*
Year made	1924	1925	1928	1937	1937	1937	1938
Service before test (years)	13	12	9	None	None	None	None
Compound	Petrolatum	Petrolatum	Rosin-mineral oil	Rosin-mineral oil	Mineral oil	Mineral oil	Rosin-mineral oi
Power factor at 13 kv and room temperature							
Initial	0.0036	0.0077	0.0042	0.0029	0.0026	0.0043	0.0040
After 60- and 80-deg cycles	0.0052	0.0069 (0.0095)	0.0043	0.0077 (0.0086)	0.0031 (0.0033)	0.0060 (0.0071)	0.0049
After 100-deg cycles	0.0057	0.0085	0.0049	0.0099	0.0037 (0.0048)	0.0074 (0.0089)	0.0067 (0.0076
After 115-deg cycles			0.0048 (0.0050)	0.0218	0.0074 (0.0087)	0.0093 (0.0109)	0.0073
After final 60-deg cycles			0.0047	0.0089	(0.0085)	0.0085	0.0062
Power factor at 24 kv and room temperature							
Initial	0.0098	0.0086	0.0043	0.0032	0.0027	0.0046	0.0055
After 60- and 80-deg cycles	0.0122 (0.0199)	0.0137 (0.0272)	0.0088	0.0106 (0.0135)	0.0031 (0.0035)	0.0061 (0.0072)	0.0049
After 100-deg cycles	0.0120 (0.0183)	0.0181 (0.0182)	0.0094	0.0129 (0.0155)	0.0044 (0.0074)	0.0078 (0.0089)	0.0074 (0.007
After 115-deg cycles			0.0099 (0.0117)	0.0302	0.0138 (0.0153)	0.0100 (0.0109)	0.0110 (0.020
After final 60-deg cycles			0.0095	0.0113	0.0137 (0.0168)	0.0105	0.0081
Power factor at 24 kv and 60 degrees centigrade							
Initial	0.0137	0.0563	0.0084	0.0038	0.0026	0.0041	0.0032
After 60- and 80-deg cycles	0.0153	0.0400	0.0100	0.0061	0.0031	0.0050	0.0043
After 100-deg cycles			0.0107	0.0069	0.0035	0.0065	0.0045
After 115-deg cycles			0.0125	0.0090	0.0062	0.0079	0.0065
After final 60-deg cycles			0.0123	0.0083	0.0101	0.0078	0.0063

NOTES: Figures in parentheses are maximum values reached in the test period. Samples *A* and *B* failed at low temperatures after the 100-degree cycles.

Table II. Tests With Pressure-Tight Potheads—Maximum Pressures

Sample	A	B	C	D	E	F	G
Maximum pressure (pounds per square inch)	6	92	85	58	110	83	107
Cycle number	10	3	1	2	1	1	7
Temperature at which maximum pressure occurred (deg C)	100	75	58	60	60	60	99

hottest conduit section along a circuit during an emergency of a day or two every year or few years, it being known that the temperatures of the remaining conduits will usually be 5 or 10 or even as much as 20 degrees centigrade lower, and that the usual cable temperatures will be materially less than the maximum permitted by existing rules in the United States.

Limitations for Low-Voltage Cable

For cable operating at 7,500 volts and less, the electrical stress is so low that the deterioration of the insulation from the standpoint of electrical strength at temperatures up to limits of $82^1/_2$ to 85 degrees centigrade set by the temperature rule is practically impossible. Since the stresses are low, there is no problem of dielectric losses.

Large amounts of cable have been in operation for 25 years or more at about 120 volts in Chicago and elsewhere, and much of it has operated at temperatures up to 105 degrees centigrade or more without serious effects on the insulation. Due to effects of the World War, carrying capacity was inadequate in Chicago for five years. During this five-year period, about 15 per cent of the two-conductor concentric cables were operated at least once a year at conductor temperatures above 125 degrees centigrade, and about seven per cent exceeded 125 degrees regularly with temperatures of 9 to 25 degrees centigrade less for the outer insulation. Some of the cable operated with copper temperatures over 200 degrees. Although the inner tapes in the insulation of some of this cable were greatly weakened, the outer tapes were almost invariably in fair condition and did not preclude successful use on reinstallation. During the past 35 years, several per cent of the 1,500,000-circular-mil single-conductor cable has operated at times at temperatures of 110 to 135 degrees centigrade without interfering with its continued operation or with its reuse after removal.

Proposed standards for this country set maximum hot-spot copper temperatures of 95 and 105–115 degrees centigrade, respectively, for continuous and emergency operation of transformers of any voltage. Even higher temperatures for emergencies have been proposed by one manufacturer[7] for transformers having an inert gas over the oil and by some operators for all transformers.

All factors considered, it seems that a reasonable limit for the insulation of low-voltage cable for emergency operation is 105–120 degrees centigrade. For so-called continuous loading, the limit should be 85–95 degrees, depending on how often and long the cable operates at the maximum temperatures.

A somewhat similar conclusion was discussed[8] before the AIEE in 1921 and was generally acceptable for practical use. The necessity for emergency operation was admitted, but some of the discussers were reluctant to provide for it in the rules. It appears that the insulation of cable manufactured during at least the past 12 years can successfully withstand operation up to the suggested limits.

Limitations for Three-Conductor 7.5- to 15-Kv Cable

Half of the high-voltage cable used in this country is three-conductor belted cable operating at 7.5 to 15 kv. For three years the Commonwealth Edison Company has been conducting accelerated aging tests on such cable to determine the effects of rare overloads on the cable on its 9- and 12-kv systems. Fifteen samples representing new cable and cable removed from the system and made by various manufacturers have been tested to the time of writing. The effective test length was usually a little over 100 feet and ended near the crotch in each pothead.

The first eight samples had test terminals filled with heavy compound exposed to open air. These terminals were not effective in preventing migration of the filling compound into the cable and did not allow the development of high pressures and vacuums in the cable.

Conditions in the middle of long lengths of cable between manholes were simulated in tests with pressure-tight terminals on the next seven samples. The amount of filling compound was reduced as much as feasible, that is, to 0.4 gallon per terminal. The terminals were filled completely with a heavy mineral oil as used in solid-type cable.

The test consisted in the application of a continuous overvoltage with superimposed heat cycles. The test voltage has been 24 kv, three-phase, 60 cycles for all samples except two. The heating was done by induced current through the three conductors, each conductor forming a one-turn short-circuited secondary on iron-core transformers placed around the samples. Heating current was applied daily for eight hours, except on Saturdays and Sundays, with cooling in open air. The nominal maximum copper temperatures began at 60 and increased as the testing proceeded to 115 degrees centigrade. After each set of high temperature cycles, one or more cycles of heating to 60 degrees were introduced to determine the effects of the higher temperatures on the normal operating characteristics, 60 degrees representing a high value of normal maximum temperatures. The duration of the aging tests was usually four weeks.

During the aging tests, measurements were made daily of power factors at test

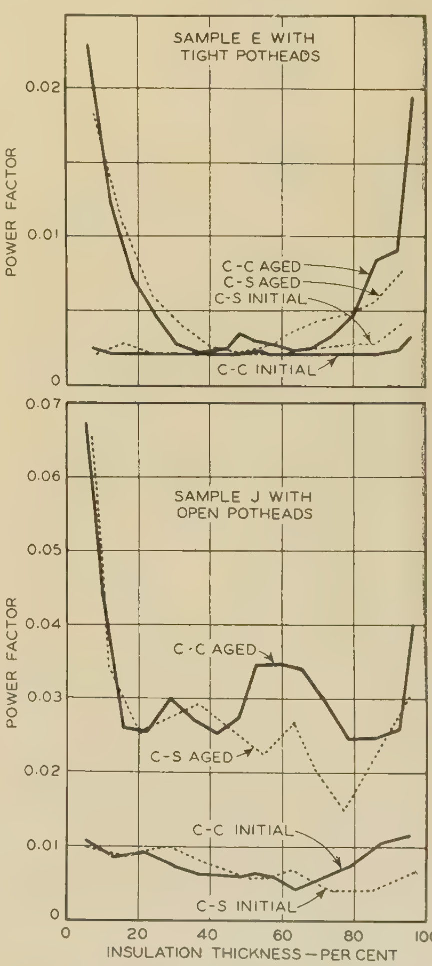


Figure 2. Changes in radial power factor at 60 degrees centigrade for 13-kv cable in accelerated aging test

C-S—Conductor to sheath
C-C—Conductor to conductor

Table III. Tests With Pressure-Tight Potheads —Pressures and Sheath Expansion

Sample	Start	After 80-Deg-C Cycles	After 100-Deg-C Cycles	After 115-Deg-C Cycles
Maximum pressures during 60-deg-C cycles*				
A	0	−4		
B	42	−5		
C	85	8	8	6
D	58	18	3	−10
E	110	11	5	−4
F	83	1	−12	−16
G	57	4	−5	−5
Minimum pressures during 60-deg-C cycles (inches of mercury)				
A	−27	−17		
B	−10	−17		
C	−18	−4	−2	−2
D	−30	−25	−28	−28
E	−20	−23	−27	−22
F	−13	−18	−19	−19
G	−18	−22	−24	−24
Increase in cable diameter at room temperature (mils)				
A		2	2	
B		6	12	
D		5	12	15
E		12	18	24
F		4	7	8
G		2	6	8

* Pressures (positive values) and vacuums (negative values) are, respectively, in pounds per square inch and inches of mercury.

voltage at room and elevated temperatures. Power factor-voltage tests were made twice a week at room temperature. In the tests with pressure-tight potheads, daily measurements were made also of maximum and minimum pressures and cable diameters. Figure 1 shows records for one sample of 500,000-circular-mil three-conductor 13-kv cable made in 1937. Such cable has sector-shaped conductors, $^{9}/_{64}$ and $^{5}/_{64}$ inch of conductor and belt insulation, respectively, and $^{8}/_{64}$-inch sheath. Before and after aging tests, samples were obtained for visual examinations and for power factor measurements of individual tapes radially through the insulation. Typical test results are shown in figure 2.

Since the tests with open and tight potheads showed some interesting differences in results, they are discussed separately.

Tests With Pressure-Tight Potheads

Table I is a summary of the tests with pressure-tight potheads. In the four new cables made recently, ionization increased during the load cycles to 100 and 115 degrees as was shown by increases in power factor at 24 kv and room temperature of 0.0063 to 0.0270 and by development of some carbon in the compound at the center of the cable. However, in subsequent cycles to 60 degrees considerable recovery in power factor occurred. Similar cables made 12 or 15 years ago have sometimes shown similar changes in normal operation. Three of the tested samples have been placed in service at 12 kv to verify over a period of years the conclusion that they will still operate satisfactorily.

One of the test samples was from 1928 cable which had been in service for about nine years, during which considerable deterioration had occurred. Although some further deterioration developed in the aging tests, the changes were not so great as those in the four new cables.

Two cables containing petrolatum, made in 1924 and 1925, had been in service for 12 to 13 years. Ionization increased considerably in both of these cables in load cycles to 60 and 80 degrees, and both failed at low temperatures after the end of the series of 100-degree cycles.

The outstanding point in these data is that, although ionization increased in all samples, the changes in power factor at rated voltage were usually small, indicating that, even if void spaces are created, little ionization occurs at normal voltage. In general, the first cycle of each temperature step seemed to have the greatest effect. An exception is the maximum power factor at 13 kv of 0.0218 for sample *D*, which occurred after cooling from 115 to 21 degrees. Such severe temperature changes will never occur in service. Furthermore, this sample recovered in subsequent 60-degree cycles so that the power factor decreased to 0.0089.

The changes in solid losses (losses in impregnated tapes only) were of minor importance compared with the increases in losses due to gaseous ionization. The power factors at room temperature and 5.6 kv increased during aging by 0.0006 to 0.0077. As table I shows, the changes in power factor at 60 degrees were mod-erate. Also, the radial power-facto curves obtained before and after agin showed small changes except near coppe and lead. The changes in the averag of the radial power-factor curves befor and after aging ranged from a decreas in power factor of 0.0127 to an increas of 0.0039.

Maximum pressures as high as 11 pounds per square inch were observed As indicated in table II, the maximum pressures did not occur at the highes temperatures. Four of the samples developed maximum pressures in the initia 60-degree cycles; none in the 115-degre cycles. The maximum pressures were apparently determined by the increase in maximum temperature over the highest previous temperature level rather than by the absolute values of the maximum temperature.

The maximum pressures decreased sharply in succeeding heat cycles at the same temperature due to expansion of the sheath. Table III shows how the maximum pressures decreased in the 60-degree cycles at various stages of the test. For the four new cables, *D*, *E*, *F*, and *G*, the pressures did not become positive at any time during the series of final 60-degree cycles, although for cable *E* this final series lasted four weeks. The minimum pressures showed little change during the aging tests in spite of the sheath expansion.

These pressure data suggest that emergency loading causing temperatures up to 100–115 degrees are not likely to produce much higher pressures than occur in the early stages of usual operation, especially since the cable will have probably carried a good load prior to the emergency. The data further indicate

Table IV. Tests on 500,000-Circular-Mil Three-Conductor 13-Kv Cable With Open Potheads

Sample	*H*	*I*	*J*	*K*	*L*
Year made	1923	1924	1927	1935	1936
Service before tests (years)	13	12	5	None	None
Compound	Petrolatum	Petrolatum	Rosin-mineral oil	Rosin-mineral oil	Mineral oil
Maximum temperature reached (deg C)	117	107	118	120	111
Power factor at 13 kv and room temperature					
Initial	0.0062	0.0047	0.0050	0.0052	0.0037
Maximum	0.0098 (103)	0.0138 (107)	0.0080 (118)	0.0099 (83)	0.0052 (111)
Final	0.0082	0.0122	0.0080	0.0084	0.0048
Ionization factor (5.6-24 kv) at room temperature					
Initial	0.0040	0.0086	0.0042	0.0006	0.0041
Maximum	0.0088 (103)	0.0202 (62)	0.0127 (118)	0.0195 (104)	
Final	0.0028	0.0024	0.0060	0.0030	0.0003
Power factor at 24 kv and 60 degrees centigrade					
Initial	0.0353	0.0159	0.0154	0.0078	0.0039
Maximum	0.0390 (103)	0.0588 (107)	0.0275 (118)	0.0277 (104)	0.0242 (111)
Final	0.0370		0.0221	0.0137	0.0214

Note: Figures in parentheses show maximum temperature in degrees centigrade after which power factor shown occurred.

the overloads will not make the vacuum after cooling more severe.

Tests With Open Potheads of Cables Made After 1920

In contrast to the tests with pressure-tight potheads, none of the cables tested with open potheads failed or showed signs of approaching instability. No high pressures or low vacuums occurred in these samples. Table IV summarizes the test results. Although the heat cycles produced some increases in ionization factor, the final values were low. The maximum ionization factors did not occur in most cases after the highest temperature steps. It appears that the decrease in viscosity of the pothead compound at the higher temperatures favored migration of compound and re-impregnation of cable insulation near the ends of the samples. For sample *L* the ionization factor was highest at the start.

The solid losses increased in all cases. In sample *L* the increase in power factor was mainly caused by the unusual migration of asphaltic compound from joints in the test leads for at least 20 feet into the test length. For other samples, the increases in power factor were probably caused mainly by deterioration at the high temperatures. The increases during aging in the average of the 60-degree power factors of the individual

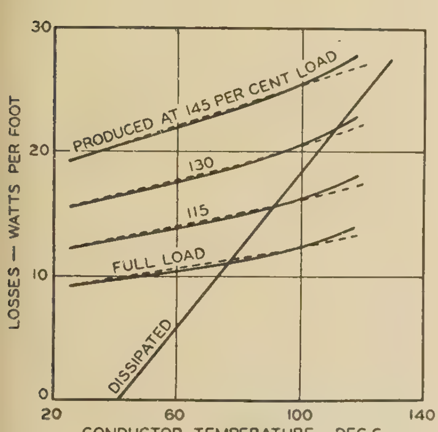


Figure 3. Thermal stability of used 500,000-circular-mil three-conductor 13-kv cable

Solid curves—Sample *J*, rosin-mineral compound cable made in 1927

Dashed curves—Sample *H*, petrolatum-compound cable made in 1923

tapes varied from 0.011 to 0.115 for four samples. For the fifth sample the average of the radial power-factor curves after aging was 0.022 less than before aging, but this difference was due probably to longitudinal nonuniformity of the quality of this sample of used cable.

Table V. Aging Tests of an Old Rosin-Impregnated Three-Conductor Cable

Approximate Maximum Temperature (Deg C)	Number of Cycles	Power Factor at Room Temperature and 15 Kv: Start	Maximum	Ionization Factor (5.6–20 Kv): Start	Maximum	Power Factor at Elevated Temperature: Start	Maximum
60	4	0.0095	0.0167	0.0107	0.0121	0.224	0.224
80	5	0.0112	0.0309	0.0075	0.0173	0.353	0.388
90	5	0.0131	0.0148	0.0163	0.0216	0.429	0.479
60	5	0.0142	0.0146	0.0208	0.0184	0.209	0.238
100–110	5	0.0145	0.0198	0.0184	0.0330	0.550	0.636
60	2	0.0187	0.0185	0.0326	0.0319	0.334	0.334

These data indicate that the changes in solid losses, even where considerable migration of asphaltic compound into the cable occurred, are not serious enough to cause failures in service, except possibly in some rare instances.

Tests With Open Potheads of Old Rosin Cables

Aging tests were made of three samples of cable made prior to 1920. They had round conductors and $^{12}/_{64}$- and $^{8}/_{64}$-inch rosin-impregnated conductor and belt insulation, respectively. One sample of 250,000-circular-mil cable failed in the first cycle after having been subjected to 24 kv for $2^1/_4$ hours and having reached a copper temperature of about 60 degrees. Another sample of similar cable withstood 36 days at 15 kv but showed considerable increases in power factor and especially in ionization factor after the first 80-degree cycle, as shown in table V.

The third sample, having number 4/0 conductors, showed signs of serious instability in the first 60-degree cycle at 15 kv, the power factor at elevated temperature rising from 0.20 after six hours of heating to nearly 0.70 after about seven hours of heating. The voltage was therefore reduced to 12 kv. The cable failed at elevated temperature in the fourth 60-degree cycle.

The two failures in this group of tests were apparently caused by thermal instability due to high dielectric losses. The impregnation of these cables was poor. The older cable, as represented by these two samples, is not considered suitable for reinstallation on the 9- and 12-kv systems, while somewhat similar cable made later (1912–18) has been so satisfactory that it is being reinstalled.

Thermal Stability

The thermal instability just discussed raises the question of instability due to overloads in service. Figure 3 shows thermal stability diagrams for two cables removed from service. The curves represent the total watts generated in the cable at various loads at operating voltage. The straight line shows the total watts which can be dissipated assuming at the start a summer duct temperature of about 40 degrees, no load for the cable in question, and usual loading for the other cables in the conduit. The intersections of the curves with the line give the copper temperatures reached for one-day emergency loads. In deriving the curves, the author has in each case used unfavorably high values of power factor in order to take into account the effect of deterioration due to aging during the overloads.

The diagrams show that such used cables are thermally stable even at 30 per cent overload. The approximate copper temperatures reached at full load, 15 per cent overload, and 30 per cent overload, respectively, are 77, 90, and 110 degrees. Lower duct temperatures would move the straight line to the left, and correspondingly higher loads would be permissible. It is of interest that, even if a power factor of about 0.20 is reached at a temperature of 115 degrees, the dielectric losses on these cables would be only seven per cent of the copper losses.

Assuming a base duct temperature of about 40 degrees, even old rosin cables would probably be thermally stable at 30 per cent overload. Since the dielectric losses were calculated from the average power factor of about 100 feet of cable, it is possible that in localized spots much higher dielectric losses may develop, especially for some of the older cables.

Joints

As the result of high loading, asphaltum-base or petrolatum joint-filling compound may become so fluid as to migrate in large amounts into the cable insulation, thereby causing increased insulation losses; but this does not seem serious, especially since migration is usually limited to cable in the manhole which is subjected to lower temperatures. A further result is pressures or vacuums in the joints which may cause serious bulging or collapsing of the joint sleeves. Most lead joint sleeves, especially for the larger cables, have been too weak me-

chanically. For Chicago 13-kv 500,000-circular-mil joints, the new practice is to use only low-loss varnished cambric insulation as applied insulation instead of insulating tubes, and to use a circular sleeve of calcium-type lead alloy, $4^{3}/_{4}$ inches inside diameter and $^{9}/_{64}$ inch thick, instead of a $5^{1}/_{2}$-inch plain lead sleeve which was ridged to provide an air space at the top. So far, the practice is to continue using an asphaltum-base compound for filling.

SUMMARY

The outstanding effect of overloads up to 115 degrees centigrade upon multiple-conductor belted cables made since 1923 was an increase in ionization; but for only two old petrolatum cables was this effect serious enough to cause failure at 24 kv. At rated voltage the changes in ionization factor were small, and considerable recovery effect was noticeable during periods at moderate temperatures following overloads. It may, therefore, be expected that the effects of emergency loading to temperatures of 100–115 degrees will not be serious and that certainly limits of 90–100 degrees are conservative.

Power factors at normal operating temperatures should not be expected to increase much due to overloads, except for the cable adjacent to joints. In general, this effect should not be expected to shorten the life of the cable.

For copper temperatures up to at least 115 degrees centigrade, thermal instability is not a factor of danger, except for badly deteriorated or old rosin insulation.

Temperature Limits for Solid-Type 69-Kv Cable

The maximum permissible temperature for the insulation of single-conductor 69-kv cable has been based in Chicago largely on accelerated aging tests and a large accumulation of operating data. The joints in service are filled with a thin oil. Halperin and Betzer[9] showed that cables of good quality withstood for as long as seven weeks, without appreciable change, tests at $2^{1}/_{2}$ times normal operating voltage and daily temperature ranges of about 35 degrees to maximum copper temperatures of 65 degrees centigrade. Insulation thicknesses were $^{40}/_{64}$ to $^{48}/_{64}$ inch. Cables of the poorest qualities (made in 1926), which are now in service, showed considerable increase in power factor in a few days of such testing. However, these changes were not sufficient to cause failure at normal voltage, and such poor-quality cables constitute only a very small percentage of the 66-kv system.

Emergency ratings for the 66-kv lines in Chicago have for a few years been based on temperatures of 60–65 degrees centigrade, depending on the quality of the cable insulation, and on a maximum allowable daily range in temperature of 18 degrees. This limiting range is necessary for the insulation of cable now in service because (*a*) deterioration of the insulation occurs almost entirely by ionization in voids produced during cooling, and (*b*) the life of the cable ma be shortened by radial expansion of th sheath as discussed later. For the yea 1937, the maximum temperatures wer 45, 50, and 55 degrees centigrade or les for, respectively, 75, 90, and 96 per cen of the lengths; and the very maximun was about 62 degrees.

Temperature Limits for the Insulation of Oil-Filled Cables

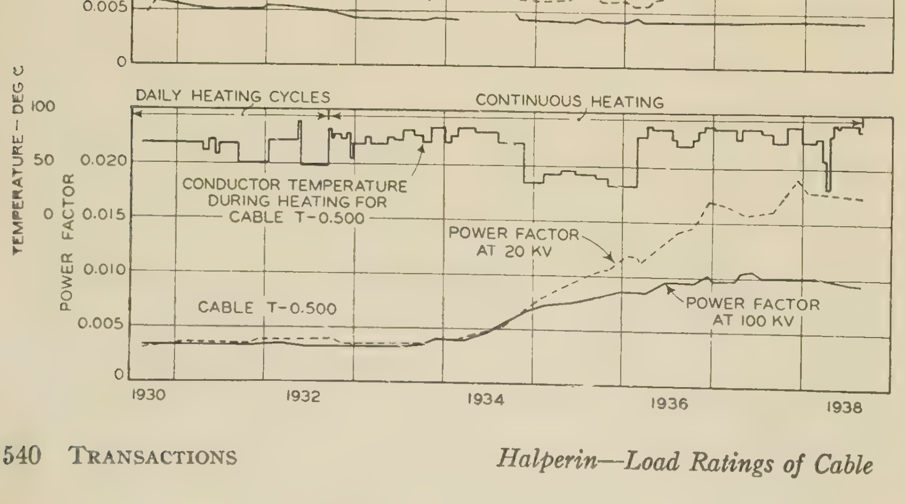
An experimental installation of oi filled cable in underground conduit i Chicago, to which brief reference[10] ha been made, consists of 1,000 feet of eac of three kinds of cable made by tw manufacturer's and designated as *R* 0.386, *S*-0.400, and *T*-0.500. The num ber is the nominal insulation thickness i inches. The cables are subjected t overvoltages, that is, to 76 kv to sheath and to surges by means of a tap from a adjacent 132-kv three-phase overhea line. Heating current is supplied b current transformers insulated for 132 kv Provision is made for measurements o power factor and average conducto temperature. The standard insulatio thicknesses for 132-kv operation wer 0.719 and 0.506 inch, respectively, i 1930 and 1938.

Testing was started in June 1930 Except for interruptions for various reasons, the cable was subjected to only voltage for one month, then to voltage with superimposed daily load cycle for two years, and subsequently to volt age and continuous heating to limi effects of daily movement on the sheath As indicated in table VI, tests have been up to average temperatures of 91 degree centigrade and to a maximum for short portions of the cable of 120 or 130 degrees At intervals of two to three weeks, testing has been interrupted to allow measure ments at both elevated and duct tem peratures of power factors at various volt ages from 20 to 100 kv.

Figure 4 shows the variations in powe factor throughout the tests for each o the three cables, all power factors being adjusted to a conductor temperature o 60 degrees, through data from frequent measurements over a range in tempera ture. Figure 5 shows the maximum and minimum changes found in power factor-temperature characteristics among these cables.

Cable *R*-0.386 has remained entirely stable throughout the test. Cable *S*-0.400, also, has been relatively stable except for a moderate increase in the power factor at 20 kv early in the test. In general, this cable has undergone

Figure 4. Power factor of experimental oil-filled cable at 60 degrees centigrade



somewhat less severe heating than cable R-0.386, but the 33 days of heating to conductor temperatures of 90–91 degrees had no apparent effect on the power factor.

Cable T-0.500 remained stable during the first three years of testing and then developed instability, as evidenced by increasing power factors. A comparison of the temperature data and power factors for this cable indicates, however, that there is no consistent relation between the increases in power factor and the testing temperature. The 365 days of heating to 85–91 degrees had no tendency to accelerate the deterioration in electrical properties since the power factors at both 20 and 100 kv showed indications of approaching a stable condition during this period.

Increases in power factor of the same peculiar type, but of much greater magnitude than those which occurred on

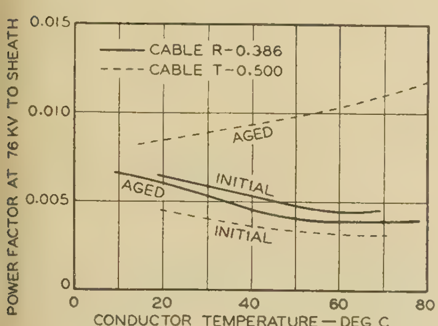


Figure 5. Change in power factor of oil-filled cable in eight years of experimental operation

cable T-0.500, were found also in one case on a portion of a commercial 132-kv line which had been subjected to only moderate temperatures. This showed that high operating temperatures are not essential for the occurrence of deterioration of this type and gave additional support to the conclusion reached above that the high temperatures to which cable T-0.500 was subjected were not in the main responsible for the instability found. In general, however, serious deterioration in oil-filled cable has been rare.

Figure 5 shows that such deterioration not only raises the power factor but changes the power factor-temperature characteristic from a relatively flat or falling curve to one which rises with increasing temperature. For cable T-0.500 the deterioration which has occurred is only a small fraction of that which would be necessary to cause failure due to cumulative heating. The increase in power factor which has occurred is, however, sufficient to reduce the carrying capacity by several per cent.

The results of these data and consideration of the earlier data in the paper and the references lead to the following conclusions:

(*a*). Changes in dielectric losses in oil-filled cable with time are not materially affected by increase in operating temperature, at least up to 90 degrees. This conclusion is not, as might be inferred, at variance with published data which indicate that the rate of deterioration in transformer insulation about doubles for each eight-degree-centigrade rise in temperature nor with the finding that rate of oxidation of oil exposed to air increases with temperature. The important deterioration in transformer insulation occurs in mechanical strength rather than in electrical properties and is caused by oxidation of the solid insulating material. The deterioration in oil-filled cable, which is usually negligible, is caused mainly by chemical changes in the oil which is in contact with only the materials sealed with it in the cable. Chemical changes of the oil do not necessarily result directly in increases in dielectric losses of the insulation.

(*b*). Oil-filled cable insulation may safely be operated continuously at copper temperatures up to 85 degrees and during emergencies at 90 degrees centigrade or more. These values are 5 to 15 degrees or more above the corresponding limits in the present Association of Edison Illuminating Companies cable specifications, where the limits already have been increased twice in the past five years.

Part II. Limitations Due to the Sheath

Radial Expansion of Sheath

As the temperature increases with load, the thermal expansion of the compound in insulation of solid-type cable causes an increase in the internal pressure and may result in serious sheath expansion. The rate of sheath expansion increases with increasing internal pressure and increasing temperature. Such matters and the properties of lead and lead alloys are covered in a recent research bulletin[11] on lead sheaths by H. F. Moore and others.

As far as load ratings are concerned, there seems to be no problem involving radial expansion of the sheath for low-voltage cable. The percentage of the volume inside the sheath that is occupied by compound is relatively small; and, because of the low electrical stresses in the insulation, void or gaseous spaces are not particularly objectionable. For cable that operates at about 12 kv and is connected with joints filled with a hard or plastic compound, the internal pressures at loads corresponding to emergency ratings may be 50 or 100 pounds per square inch, as shown in the accelerated aging tests. Several cycles of emergency loading during the life of such cable may produce sheath expansion of 10 to 30 mils, but such expansion is small compared to the expansion that has been found on 69-kv single-conductor cable in Chicago which has operated successfully to date. It, therefore, seems that no trouble due to sheath expansion will occur on moderate-voltage cable.

Table VI. Tests of Experimental 132-Kv Oil-Filled Cable in Chicago From Start to July 8, 1938

Cable	R-0.386	S-0.400	T-0.500
Total elapsed time (days)	2,934	2,934	2,934
Days of voltage application (76 kv to ground)	2,105.1	2,222.5	2,457.8
Number of daily load cycles to:			
Less than 65 deg C	197	99	216
65 to 74.9 deg C	364	462	345
75 to 79.9 deg C	0	1*	0
80 to 84.9 deg C	0	0	0
85 to 89.9 deg C	1*	0	1*
Total	562	562	562
Days of continuous heating to:			
Less than 65 deg C	49.8	265.0	216.9
65 to 74.9 deg C	592.8	371.2	464.7
75 to 79.9 deg C	590.5	222.6	308.2
80 to 84.9 deg C	142.4	19.6	235.3
85 to 89.9 deg C	48.7	0	336.8
90 to 91 deg C	27.8	32.9	27.8
Total	1,452.0	911.3	1,589.7

* During this cycle the heating current was applied continuously for nine days instead of the normal time of 12 hours.

Note: Copper temperatures are average values determined by resistance measurements. A recent longitudinal survey of duct temperatures revealed extremely large variations in temperatures—a condition never before even approached in Chicago except where steam mains were present. This conduit was installed for test purposes in a prairie and the condition and thickness of the soil covering varies greatly. It is estimated that for an average copper temperature of 90 degrees centigrade the maximum at one localized region was 120–130 degrees.

If 69-kv solid-type cable with oil-filled joints is loaded rather heavily soon after installation, the internal pressure rises until the sheath is stretched enough to accommodate the heat-expanded insulation; and the pressures may be 50 or 75 pounds per square inch or more. Under usual loading conditions common daily maximum pressures are one-third or one-half of these values. As the sheath expands, oil enters the cable from the joints. For the 280 miles of such cable in Chicago that has been in service up to 12 years, the rate of expansion of the cable with $^9/_{64}$-inch sheath is about six mils in diameter per year, and this rate does not appear to be decreasing. For a relatively small amount of the cable in which $^8/_{64}$-inch sheath was used, the rate of sheath expansion has been roughly twice as much.

In the first few years of service, open-

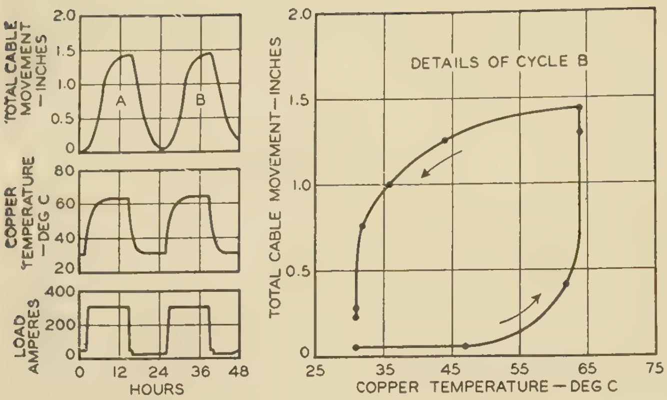


Figure 6. Cable movement for line with block-type loading

For 391-foot length of 350,000-circular-mil three-conductor 13-kv cable

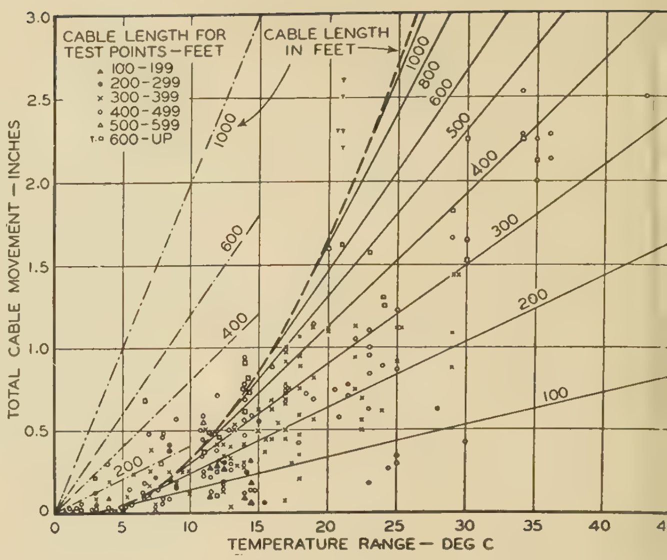


Figure 7. Theoretical and actual cable move-ment

Data are for 500,000-circular-mil three-con-ductor 13-kv cable. Lines at left are for cop-per-bar expansion, while lines at right ar values of cable movement derived from formul

ings developed mainly at defects in the sheath structure. With further service, openings developed at the thinnest portion of sheaths that varied considerably in thickness around the circumference. In most of these cases the minimum thickness of the sheath initially was less than 85 per cent of the average thickness. Sheaths produced prior to 1930 developed high rates of trouble when the average expansion around the circumference exceeded about three per cent. More-recently-made sheaths are better in quality and in concentricity and should withstand internal pressures much better.

Accelerated aging tests[9] have shown that as far as the insulation is concerned the daily temperature range for the 69-kv cable could be increased from the present limit of 18 degrees centigrade to 30 or 35 degrees. Such an increase is not feasible for solid-type cable with thick insulation and connected with oil-filled joints unless a sheath with increased creep resistance is provided.

Induced Sheath Potentials on Single-Conductor Cable

For single-conductor cable installed with insulating sleeves and bonded to eliminate sheath losses, the induced sheath potentials vary directly with the magnitude of the alternating current and directly with the distance between insulating sleeves. In 1929 it was suggested in an AIEE paper[12] that, on the basis of laboratory and field experience, the limiting a-c potential between sheath and ground should be 12 volts in order to avoid a-c electrolysis. The general idea then was that this limit would apply during the time of the maximum expected load.

Since then, in order to provide further field data on the effects of the a-c potentials in causing electrolytic sheath corrosion, fireproofing has been removed periodically in many manholes from cable that has been submerged. No corrosion was found except in one case where the sheath also had a slight positive d-c potential on it inadvertently. These cables in general had operated with a-c potentials up to about ten volts. In view of these and other data, it was decided in 1935 that the maximum safe induced sheath potentials to ground for the usual daily loading should be 11 volts and that during emergencies the potential could go to 15 or 16 volts. When the loadings cause higher potentials than these values with cross or auxiliary cable bonding,[12] it is necessary to use sheath-bonding transformers to make the sheath potential to ground one-half of the induced sheath potential in the length of cable between insulating sleeves. Even with bonding transformers and ordinary spacing of the ducts, the usual and maximum loadings may be limited to approximately 875 and 1,250 amperes, respectively, in case the sections of cable are about 600 feet long.

Cable Movement

It has been accepted usually that in the determination of the load rating of a cable the allowable copper temperature should be based solely on characteristics of the insulation, although there has been some discussion of the possibility that the life of the sheath should also be considered. The noticeable number of sheath cracks found especially in cable which has been installed for ten years or more has served to put added emphasis on consideration of the life of the sheath and has suggested that limiting range of copper temperature based on allowabl cable movement should be established

Since no previous published attempt have been made to establish practica working relationships among sheath life cable movement, and load ratings, i has been necessary to start at the be ginning and see what could be learne (*a*) about the relation between cabl movement and loading and (*b*) about th relation between cable movement an sheath life. Operating and laborator data bearing on these points have bee collected and studied for over 12 years i Chicago. The results of these studie have provided a basis for quantitativ discussion of both phases of the problem

Cable Movement as a Function of Load Range

It has generally been assumed tha cable movement should be directly pro portional to temperature change and t cable length, allowing for a constan percentage of reduction of movement du to flexing or snaking of the cable in th ducts. Field data, however, have faile to corroborate this assumption. Thi was thought to be due to the maskin influence of the many indeterminate dis turbing factors which are present in ac tual underground systems. It now ap pears that by taking into account th *restraining forces* due to friction in th duct and to the training of the cable i the manholes, the field data on cabl

movement become intelligible. A review of the theory will be given.

Consider a length of cable in a duct in which neither tension nor compression is present. Evidently a reduction in the temperature of the cable, which tends to cause it to contract, must develop enough tension to "pull" the length of cable between the manhole and any given point before there can be any movement at that point. The maximum possible tension which could be developed is that required to move the cable along the entire length from the manhole to the center of a duct run. The effect of a temperature reduction cannot be manifested entirely as contraction of the cable, since some of it appears as tension.

Assume now that the maximum tension which can be developed by load cycles and friction in a duct run is present when heating starts. Part of the thermal expansion relieves tension. Part of it tends to push the cable out of the duct mouth, but this takes force, which can be developed only by compresssion of the cable and must be great enough to "push" the portion of cable which actually moves. As heating progresses, the remaining tension becomes localized nearer and nearer the center of the length,

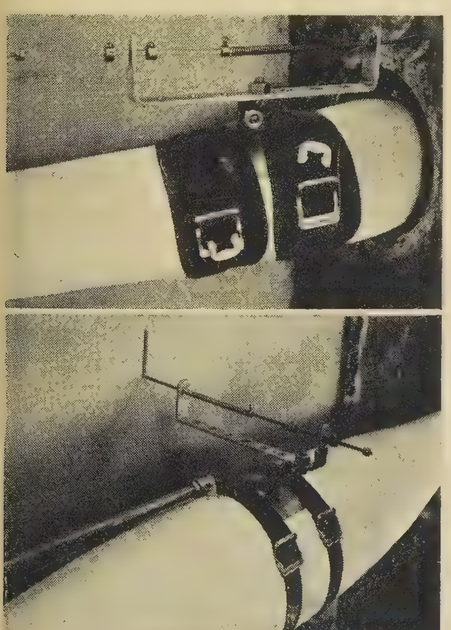


Figure 8. Cable-movement indicator

Top—Indicator installed to measure cable movement at a duct mouth. The range in movement over a period of time is obtained by measuring the separation of the two riders shown on the rod which butts against the duct wall

Bottom—Indicator installed on a cable joint to determine the limits of the lateral movement of the joint to and from the manhole wall

while the cable near the ends becomes compressed and produces movement. Finally, maximum compression is developed, it being that force which is just sufficient to push all the cable from the center toward either end. The actual movement appearing at the duct mouth is that portion of the expansion remaining after the tension and compression requirements are fulfilled.

These points are illustrated in figure 6, which gives movement data obtained on a certain well-loaded 12-kv three-conductor line, which had unusually abrupt changes in load. During the first portion of the load cycle, the increase in copper temperature does not result in any appreciable movement at the duct mouths. Instead, the cable becomes compressed. As the temperature increases, the forces developed finally exceed the force required to push the cable through the duct, and movement takes place at the duct mouths. On the cooling cycle, considerable drop in copper temperature occurs before the cable commences to move back into the duct. The copper temperature must decrease enough to relieve the existing compression in the cable, and then must build up sufficient tension to overcome the frictional forces tending to prevent the retraction of the cable.

In the equations for cable movement, the following symbols are used:

M = total movement in inches for a length of cable
L = length of duct in inches
C = coefficient of thermal expansion of cable (16.7×10^{-6} approximately)
A = cross-sectional area of copper in circular inches
W = weight of cable in pounds per inch
D = coefficient of friction for cable in the duct
E = Young's modulus of elasticity for cable in pounds per circular inch ($15 \times 10^6 \times \pi/4$ approximately)
k = maximum longitudinal stress which may exist in the cable at the duct mouth, due to the restraining force of the cable in the manhole
T = copper temperature change in degrees centigrade
T_c = $(WDL + 2k)/AEC$, the temperature change necessary to produce the maximum possible change in stress throughout the cable

If a condition of maximum *compression* were present at the start of a heating cycle, the total movement would be the same as for the thermal expansion of a copper bar, that is,

$$M = LTC \text{ inches} \tag{1}$$

Under actual conditions of normal cyclic movement, the state of maximum compression or of no initial strain would never be present at the start of a heating cycle. Either part or all of the cable would be under maximum tension. For this condition, which is characteristic of uniform cyclic loading, the cable movement is given by the following formula:

$$M = AEC^2(T - 2k/AEC)^2/2WD \text{ inches} \tag{2}$$

This formula is to be used up to the point where $T = T_c$. At higher temperatures the formula to be used is

$$M = LC(T - T_c/2 - k/AEC) \tag{3}$$

Equation 3 takes account of the temperature rise being more than sufficient to overcome the effects of tension, compression, and the restraining force k in the manhole. Neither equation 2 nor 3 takes account of flexing. The cable is considered to act as an elastic column. Numerical values for use in these equations are readily available except for the variables C, k, and D.

The coefficient of thermal expansion of cable might differ somewhat from the coefficient of expansion of a copper bar. The coefficient C for cable would therefore include, for example, the effect of buckling strands or restraint due to the sheath. No definite evidence that buckling occurs has ever been found in Chicago. The sheath does not appear to offer much restraint. The temperature change of the sheath is less than for the conductor, but this is almost exactly offset under stable conditions by the greater coefficient of thermal expansion for the lead sheath. The lag of the sheath temperature behind the copper temperature in normal load cycles may have some slight effect, but calculations confirmed by one laboratory test show that C for cable in ducts that are not submerged is about the same as for copper, that is, $C = 16.7 \times 10^{-6}$ approximately.

The restraining influence k of the cable in the manholes is variable and data on it are scanty. For typical horizontal and vertical offsets of the joint with respect to the duct mouth, the force is estimated from one full-scale laboratory test to be roughly 300 pounds for a three-conductor 500,000-circular-mil 13-kv cable. It is assumed that the force of 300 pounds is built up at some part of the heating cycle to oppose the expansion of the cable into the manhole; then, as the cable cools down, the force drops to zero and builds up to 300 pounds in the opposite direction. An installation of cable having small offsets in the manhole would require a greater force to cause

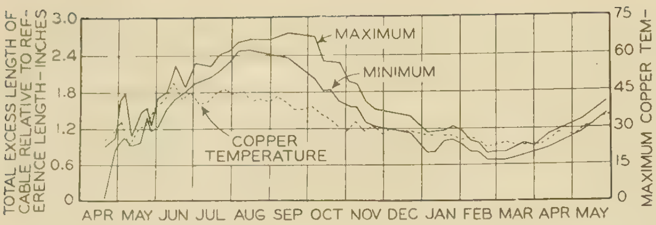


Figure 9. Weekly data for cable movement over one year

For 506-foot length of 69-kv 2,100,000-circular-mil single-conductor solid-type cable

bending than one with large offsets, and the movement would be correspondingly less. The flexibility of the fireproofing used on the cable in the manhole has a marked influence.

The coefficient of friction of cable during installation may vary greatly with different cable and duct materials, but a fair average value, based on many measurements of pulling strains during the past 12 years, is 0.5. This value has been used for D, and is probably accurate enough for any length of cable being pulled or pushed in a duct by cable movement, except where a long length is being pushed. In the latter case, it would be necessary also to include the effect of flexing in a duct and the resultant component of pressure against the sides of the duct. This has not been included, as no values for such conditions were available. The friction coefficient under service conditions may be somewhat reduced where there is vibration of the conduit.

Figure 7 shows curves from equations 1 and 3 for one type of cable. Field data are shown for comparison. The straight lines emanating from the origin are for the theoretical expansion of copper bar, which is given by equation 1. The parabolic fan of lines to the right is obtained from equation 3. The dashed line is the calculated movement that would occur on an *infinite* length of cable; for example, regardless of length the total movement at the two ends could never exceed 1.7 inches for a temperature range of 20 degrees centigrade. This is not due to flexing, since equation 3 is not set up to take account of it. It is due solely to the fact that a large percentage of the thermal expansion is taken up by longitudinal compression or tension in the cable.

If flexing were taken into account, the calculated movement would be even smaller. It might be still further reduced by the occurrence of enough flexing to make the cable anchor itself firmly in the duct at widely-separated points, thereby reducing the effective cable length. Although these phenomena are known to occur, the theory is conservative in not taking them into account.

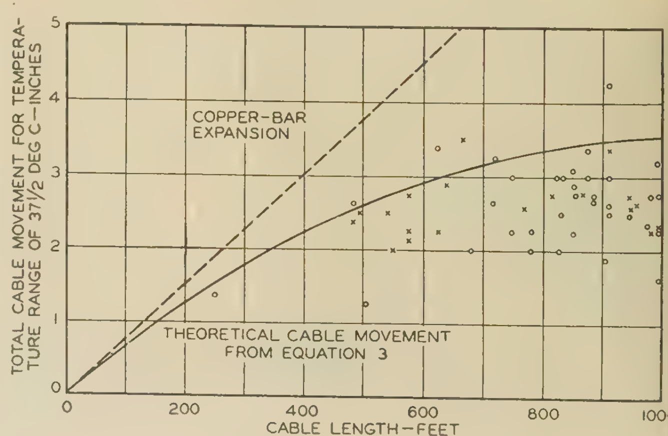
The movement follows the dashed line as long as compression is building up in the cable. When the maximum possible compression for the length under consideration has been reached, the movement thereafter becomes directly proportional to temperature rise and diverges along a straight line. The point of divergence occurs at large temperature rises for the longer lengths, for example, 14 degrees for a 600-foot length. This means that no matter how long a length of cable may be installed, its movement will be no greater than that of a 600-foot length, as long as the temperature range does not exceed 14 degrees. After the point of divergence is passed, longer lengths move more than short ones; but the 1,000-foot length in figure 7, for example, moves only 46 per cent more than the 500-foot length for a temperature range of 25 degrees.

The difference between one type of cable and another is negligible as long as the ratio of conductor cross section to total cable weight is similar. The calculated movement for 69-kv 2,100,000-circular-mil single-conductor cable and 5-kv 375,000-circular-mil three-conductor cable is about the same as for the 13-kv cable in figure 7. It is much less for heavy cables with small conductors, such as 132-kv 600,000-circular-mil single-conductor cable with 719 mils of insulation.

Figure 10. Cable movement on a 69-kv line of the Cincinnati Gas and Electric Company

⊙—Slope of conduit three per cent or less

X—Slope of three to six per cent



The conclusions derived from the theory are strongly supported by the field data obtained on various cables at more than 250 locations during the past 15 years. Usually the movement has been measured in adjacent manholes at the two ends of the length. In some cases the lateral movement of the joint has been obtained also. Data have been gathered at each location for periods ranging from one day to one year. The instruments used for obtaining complete records such as shown in figure 6 are graphic recorders. Where only the limits of the movement are desired, the indicator shown in figure 8 is used. Since this is more convenient to install and maintain than the recording devices a large number have been put into use.

A continuous record of cable movement for a year is shown in figure 9 for a length of 69-kv single-conductor cable. The record illustrates that movement has two components, the daily movement due to load cycles and the annual movement due to changes in ground temperature. The two solid lines form the envelope of the daily oscillations which are superimposed on the seasonal cycle. Both the seasonal and daily movements are fairly well distributed between the two ends of the cable. Field data obtained in many cases indicate that the probable distribution of movement between the ends of a length is 60 and 40 per cent, although a 50-per-cent distribution is not uncommon, and, in a few cases for small movements, the entire movement of a length will appear at one end. The assumption used in Chicago is a 60- and 40-per-cent division of the total movement.

The annual movement is usually larger than the daily, but obviously only the daily movement is of importance in causing sheath cracks. The annual movement may have some indirect effect such as changing the training conditions in the manhole or forcing the joint against the wall. In Chicago the seasonal range in ground temperature is about 18 degrees

centigrade. The usual daily range in copper temperature for 500,000-circular-mil three-conductor 13-kv cables has been about 8 degrees centigrade although it has on rare occasions reached 35 or 45 degrees.

Figure 7 shows that actual daily cable movement is usually less than indicated by equation 3, particularly for the higher temperature ranges. Some test points in the lower temperature ranges are higher than the calculated values, but this is partly due to errors in estimating the temperatures as indicated elsewhere in the paper; the cable movements involved, moreover, are small and relatively unimportant. The few recorded cable movements for the higher ranges that lie above the lines given by equation 3 are probably for cables installed under different conditions than were assumed—that is, less friction in the duct, smaller restraining forces in the manholes, a different initial state of tension or compression, or a combination of these factors. To illustrate, data designated by T for 69-kv 2,100,000-circular-mil single-conductor cable have been inserted in figure 7. These points are all for 600-foot lengths of cable which were installed in winter and left without load until the middle of summer. Thus they were subjected to sufficient seasonal rise in ground temperature to approach maximum compression. Upon application of the first load cycle they would tend to obey equation 1; that is, the movements should be about the same as the theoretical expansion of bar copper, and so they were. Subsequent daily movements have been normal, that is, much lower.

The daily movement is less, in general, than calculated for all the many types of cables installed in Chicago, as illustrated for the 13-kv cable in figure 7. This means it is safe to accept the calculated values as an upper limit of daily movement. It strongly indicates also that flexing or anchoring of the cable in the duct often occurs. Undoubtedly other factors, such as abnormally high values of duct friction and restraint in manholes, help to decrease the movement in some cases, but certainly not in all cases. For some of the very low values plotted for the 400- and 500-foot lengths, the field records show that practically no movement occurred at one end of the length, which definitely indicates flexing and anchoring. Another such indication is the wide spread in recorded values of movement for any one length and temperature range; for example, for 300–400 feet and 18 degrees the movements range from 0.2 inch to 1.06 inch. Any assumptions that now appear reasonable concerning variations in duct friction and manhole restraint can account for only part of this spread.

It is concluded that there is no objection, from the standpoint of cable movement, to the installation of cable lengths that are much longer than have usually been considered practicable in the past.

Some installations of long single lengths of cable in normal underground ducts have been made in Cincinnati. The cable is 500,000-circular-mil, single-conductor, 69-kv, oil-filled with 315 mils insulation and $^{8}/_{64}$-inch lead sheath. Cable movement data, for which the author is indebted to the Cincinnati Gas and Electric Company, are shown in figure 10, along with the theoretical movement calculated from equation 3. The cable movement was entirely in accordance with what would be expected from the previous discussions herein; for example, the movement for 900–1,000-foot lengths is only slightly greater than for 500-foot lengths, and the movement is generally much less than for 13-kv 500,000-circular-mil three-conductor cable in which the ratio of copper cross-section to cable weight is relatively much greater. These data are of interest also because some of the conduit sloped considerably, while Chicago conduits are almost always horizontal. There was a tendency for the cable movement at the downhill end of a sloping length to exceed the movement at the uphill end, but no permanent downhill migration was found. This is in accordance with the theory which indicates that downhill creep would occur only for shorter cable lengths or larger temperature ranges than prevail in Cincinnati.

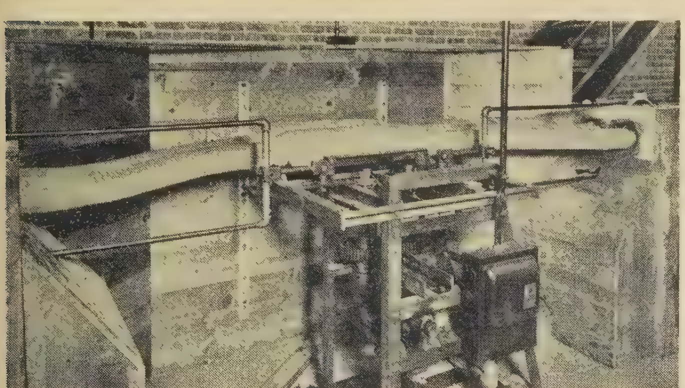


Figure 11. Dummy manhole apparatus

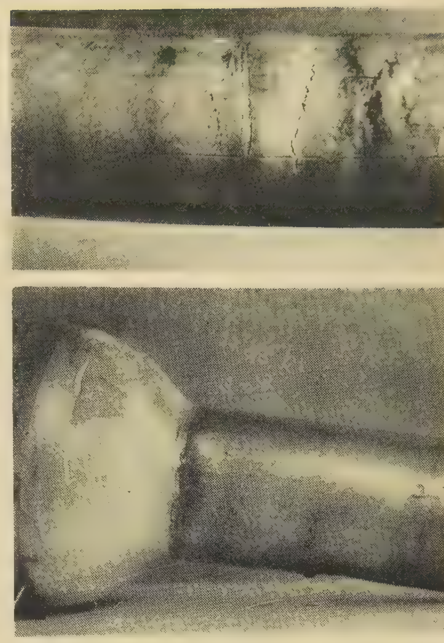


Figure 12. Sheath cracks developed in the dummy manhole tests

Top—Cracks on straight cable near the duct mouth

Bottom—Cracks adjacent to the joint wipe

Sheath Life

During 1920–24, the importance of sheath cracks on the Chicago underground system was coming to be fully appreciated, partly because the number of sheath repairs and line failures due to cracks was too high. In 1925, inspectors examined as many of the 24,000 manholes as was feasible, with the result that 500 or 600 sheath cracks were found. Many of the manholes were found to be too small from the standpoint of cable movement, and the protection at duct mouths and supports was found to be inadequate to prevent wearing of the sheath. A program for remedying these deficiencies was instituted and has since been vigorously followed, with the result that two-thirds of the present manholes conform to present standards. The present standards for straight-type manhole sizes are 8 feet by $4^{1}/_{2}$ feet for manholes

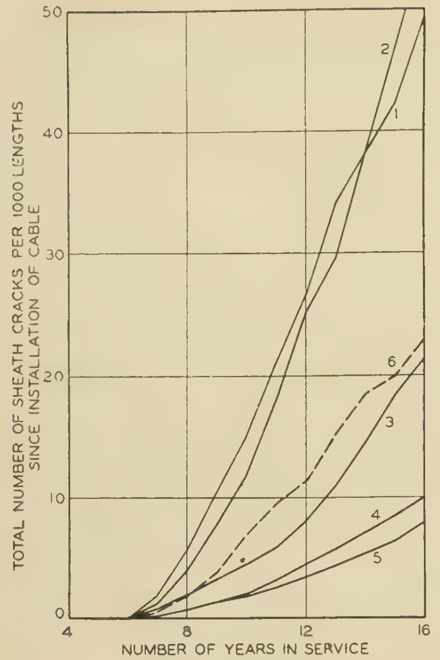


Figure 13. Sheath cracks on 500,000-circular-mil three-conductor 13-kv cable

1—Lengths 400–500 feet
2—Lengths 300–400 feet
3—Lengths 200–300 feet
4—Lengths 100–200 feet
5—Lengths 0–100 feet
6—Total rate for all lengths

to contain three-conductor 5-kv cable with conductor sizes of 375,000 circular mils or less, 10 feet by 6 feet for three-conductor 500,000- or 650,000-circular-mil 13-kv cable, $11^1/_2$ feet by 6 feet for 750,000- or 1,000,000-circular-mil solid-type 69-kv cable, and 13 feet by $6^1/_2$ feet for 2,100,000-circular-mill solid-type and oil-filled-type 69-kv cable. The minimum headroom is 6 feet in all cases.

As a result of the rehabilitation program, the number of sheath cracks found per year has been cut approximately in half. About 86 per cent of the sheath cracks on the transmission system are found and repaired before they develop into electrical failures. The methods of repair have evolved and improved somewhat with time. At best, however, operating experience has demonstrated that many of the repairs are satisfactory for only a limited time.

Laboratory studies carried on along with the rehabilitation program have consisted mainly of tests made in the full-sized "dummy" manhole illustrated in figure 11. The manhole is of the standard octagonal shape used in Chicago, with the ducts at the middle of the end walls. The length is adjustable. In a test, a 20- to 25-foot length of cable is used. The training, jointing, and fireproofing are varied to suit the test, but are done in accordance with field procedure. A motor-driven device imparts the desired amount of reciprocating motion to the cable at both duct mouths in order to simulate cable movement. The duration of a test cycle is either 70 or 110 seconds. Three-hundred-twelve cycles are taken as the equivalent of one year of life. In order to indicate sheath failure, oil pressure is supplied to the joint. The appearance of oil or cable compound shows a sheath crack. Figure 12 illustrates the appearance of the cracks. They are similar to cracks developed in sheaths in service and their division among joint wipes, duct mouths, and bends is about the same.

Sixty-seven tests have been made and more are planned. Many of the test results are not directly applicable to problems in the present study. However, they confirm, in general, the proposition that sheath life is greater for longer manholes and for greater offsets between axes of joints and cable at the duct, but the data are not sufficient to determine numerical relationships. Some calculations, however, indicate that an increase in offset is two or three times as effective, within limits, in prolonging sheath life as an equal increase in length of bends. This benefit of increased offsets is based on no accompanying increase in concentration of bending at the duct mouth and joint wipe. Tests indicate that sheath life is shortened by concentration of bending as occurs in service. Studies are being made of schemes to prolong the sheath life in small manholes that cannot be enlarged.

Some definite figures on the life of various sheaths under certain conditions have been obtained. The findings for two types of sheaths on 13-kv and 69-kv cables installed in "standard" sized manholes are summarized in table VII. The average life of commercially pure sheath of 13-kv three-conductor 500,000-circular-mil cable is about 20 years, when installed in a manhole ten feet long from duct to duct, with an offset of 19 inches (18 inches horizontal, 6 inches vertical), with a cable movement of 0.75 inch at each duct mouth and with the joint free to move on the supporting bracket.

The duty on the sheaths of cable in actual service has been considerably less severe than in the tests as far as cable movement is concerned, but more severe with respect to manhole conditions, especially for lines which were installed in manholes built 12 or more years ago. Different lines have, of course, been subjected to different conditions, especially as to loading.

Table VII. Life of Sheaths in Dummy Manhole Tests

Kind of sheath	Commercially pure lead	Calcium-type lead alloys
Kind of cable	500,000-circular-mil, three-conductor, 13-kv	
Number of tests	9	2
Sheath life in years:		
Minimum	9.6	31.6
Average	20.8	38.3
Maximum	34.5	45.0
Kind of cable	750,000-circular-mil, single-conductor, 69-kv	
Number of tests	4	5
Sheath life in years:		
Minimum	9.6	38.2
Average	18.5	67.8+
Maximum	23.8	80.0+

Note: Movement at each duct mouth was about 0.75 inch. The sheathing of cable with calcium-type alloys was done on an experimental basis.

The effect of differences in loading is seen in the fact that 45 per cent of the cracks that occurred in 500,000-circular-mil three-conductor cable operating at 12 kv during the five-year period 1933–37 were confined to the 13 per cent of the lines which carried the heaviest loads. Obviously, some lines must carry relatively heavy loads. These lines had normal cable movement for daily temperature ranges of about 15 degrees centigrade during the 11-year period 1927–37, whereas the average range for the remaining 87 per cent of the 12-kv lines in the same period was only 7 degrees, with correspondingly less movement. The effect of small manholes and of inadequate protection at duct mouths and supports

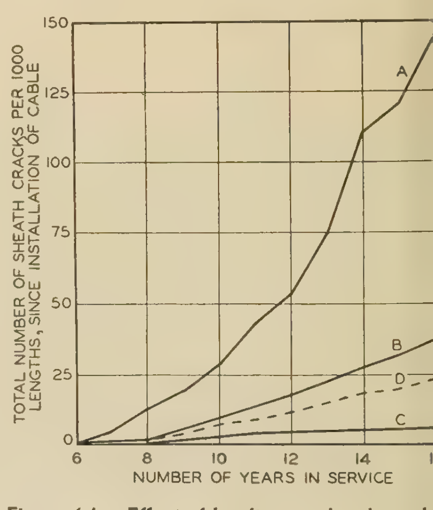


Figure 14. Effect of loading on sheath cracks in 500,000-circular-mil three-conductor 13-kv cable

Average Loading of Lines During 1927–1937 (Amperes)

Curve	Range	Mean
A	250–315	277
B	200–249	225
C	Up to 200	175
D	All lines	205

was to shorten the life of the cable. In spite of the rehabilitation program, the effects of previous conditions are still being felt. For example, in 21 out of 29 typical cases of sheath cracks occurring in 1937, there were prominent contributing factors such as small manholes, wearing at old duct mouth before manhole was enlarged and adequate protection provided, and abnormalities in sheaths such as solder patches applied for bond-wire connections or repairs of former cracks. All of the 29 cables involved were 10 to 15 years old.

Similar findings in varying degrees apply also to the cables operating in Chicago at 120 to 33,000 volts, appreciable percentages of which were installed before protection and training had been improved. They do not apply to the 66-kv and 132-kv cables, which were all satisfactorily protected against wearing and were installed largely in manholes that still appear to be of sufficient size for the usual daily temperature ranges of 5 to 15 degrees centigrade that have prevailed. The measured daily cable movement has been about 0.1 to 0.5 inch at each duct mouth, with an average of about 0.25 inch. Assuming that the sheath life is inversely proportional to the amount of daily movement, then, according to table VII, an average sheath life of about 50 years would be expected, with a possible minimum of about 25 years. It is still too soon to tell what the actual sheath life will be, although a few cracks have occurred, most of them at bond wipes or where the sheath was damaged. The oldest lines have been in service 12 years.

The life of 13-kv cable sheaths has been studied to determine the influence of the age, loading, and length of the cable, with the results shown in figures 13 and 14. About 7 or 8 years after installation a few lengths developed sheath cracks. After 15 years of service the number of cracks has been about two per cent of the number of lengths, some lengths having more than one crack. The longer lengths developed about twice as many cracks as the short lengths, but lengths over 400 feet did not have many more cracks than those between 300 and 400 feet. This is to be expected from the relative amounts of cable movement. Also, the lengths over 400 feet are often part of installations in exceptionally favorable locations. For example, such lengths are common on 12-kv lines installed along with 66-kv lines, for which especially long manholes are provided.

The rate of cracking was much higher on the lines that had higher average loads. Figure 14 shows that the group of lines having the highest average weekday loads (277 amperes) developed about 130 cracks per 1,000 lengths after 15 years of service compared to 30 cracks per 1,000 lengths for moderately-loaded lines (225 amperes average) and 5.6 cracks per 1,000 lengths for lightly-loaded lines (175 amperes average). In this study the load for each line was treated as follows:

(*a*). The maximum three-hour average load for each week in the year was taken from the records; and the average of these 52 values was called the yearly average.

(*b*). The yearly average was determined for each of the 11 years in the period 1927–37, inclusive.

(*c*). The over-all average of the 11 yearly averages was used for the average load on each line.

The individual lines were grouped in accordance with their average loads, as indicated in figure 14, which shows the mean for each group and also the grand average for all lines.

One very important application of the findings on cable movement and sheath life is the determination of the effect of increased loading. For example, the loading on the 13-kv cables in Chicago is

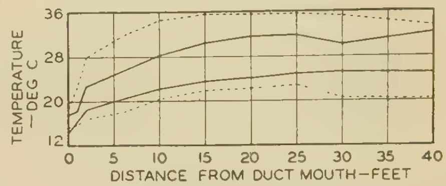


Figure 15. Temperature gradients of empty ducts at typical locations

being increased to provide greater transmission efficiency. An increase over the past 11-year loading of about 18 per cent in the average load is expected for the 87 per cent of the lines that carry the lighter loads. The increase for the remaining lines, which carry the heavier loads, will probably be held to about 8 per cent to limit sheath cracking. The probable effect on the life of the cable sheaths over a period of years is indicated in table VIII.

The increased rate of sheath cracking, excluding the effects of increasing age, would boost the total rate of cable failures from 3.2 to about 3.8 failures per 100 miles per year, assuming that the present efficiency of locating and repairing sheath cracks will be maintained and no substantial changes in manhole conditions take place. The increase does not appear alarming at first, but it is to be expected that most of the additional failures will appear on the more heavily loaded lines and result in a high rate for those lines. There are reasons for supposing that the life of sheath would decrease somewhat faster than the cable movement increases. There would be a considerable increase, also, in the time and expense of repairing sheath cracks. On some of the heavily-loaded lines, cable replacements might be necessitated because of excessive cracking in certain manholes.

The present study is limited to about 15 years because of scanty data on older cables. For this reason, any figure for the total sheath cracks on older cables could be obtained only by extrapolation on figure 14, with possible error. The number of sheath cracks on the oldest cables having the heaviest loads is probably higher than normal now due to lingering effects of former manhole conditions. These effects will gradually disappear as the cables with damaged

Table VIII. Effect of Increased Loads on Sheath Life of 500,000-Circular-Mil Three-Conductor Cable Operating at 12 Kv

	Average of Lighter-Loaded Lines (87 Per Cent of Total)		Average of Heavier-Loaded Lines	
	Former Conditions	Future Conditions	Former Conditions	Future Conditions
Average of weekday maximum loads (amperes)	195	230	277	300
Daily range in copper temperature (deg C)	7	10	15	18
Average cable movement at a duct mouth in inches:				
0–200-foot lengths	0.025	0.045	0.07	0.09
200–300-foot lengths	0.05	0.11	0.204	0.26
300–400-foot lengths	0.06	0.16	0.312	0.40
400–500-foot lengths	0.06	0.19	0.400	0.52
over 500-foot lengths	0.06	0.19	0.456	0.625
Approximate number of sheath cracks in per cent of lengths in 15 years:				
In 200–300-foot lengths	1.0	2.6	9.2	14
In 400-foot and longer lengths	1.4	6.8	45*	65*
In all lengths	1.3	3.4	13.0	19

* Based on relatively meager data.

sheaths are weeded out, but an upward trend in sheath cracks and service failures with increased loading and age will remain.

The magnitude of movement which is producing sheath cracks is 0.6 inch and less at each duct mouth for almost all of the cases involved. This movement is less than might have been expected to cause such cracks and less than that which gave an average sheath life of 20 years in dummy manhole tests.

One way of obtaining increased loading efficiency without seriously reducing the sheath life would be to make the manholes still longer and wider, notwithstanding the small amount of movement involved. This has the obvious disadvantages of requiring great time and expense and of not being always feasible owing to lack of space in the streets. Another method is to try to improve other conditions affecting movement and cracking, but the prospects are not too bright for such a solution. A better method is to obtain more resistant sheath.

The results of the dummy manhole tests (see table VII) show definitely that the life of calcium-type lead alloy sheaths, which were furnished on an experimental basis, is at least twice as great as for commercially pure lead. Such alloy sheaths have outstanding resistance also to radial creep and to abrasion, making for an ideal combination of qualities. Lead containing two per cent tin was no more resistant to bending in a few tests than commercially pure lead, and antimony alloy was less resistant.

It seems that for many cables the expected time of present-day commercial sheaths to cracking in manholes is only one-third to two-thirds of the life of the insulation, at least for Chicago. Apparently better sheath materials are needed to resist the effects of cable movement. Then a *balanced* cable design will become available, but it would be many years before the full advantage of installing such cable could be realized on an underground system.

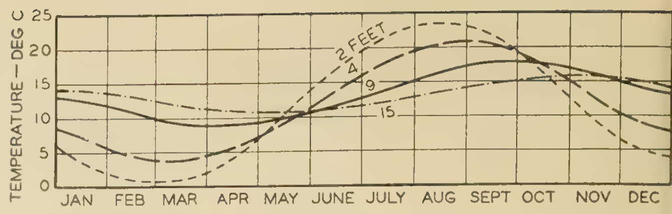


Figure 17. Seasonal variation in ground temperature at various depths

Curve for four feet is used as general ambient

Part III. Heating Characteristics of Conduits

Some work on determining temperatures of conduits has been done in Chicago since 1910. From 1923 on, however, such work has been unusual in that it has been on a *continuous* basis for the entire city and has been supplemented with many special investigations. The resultant data have afforded a continuous basis for determining the allowable loading of cables and the allowable number of cables to put into a given conduit, without the necessity for large factors of safety to take care of unusual or unforeseen variations in the heating characteristics and without danger of the conduit temperatures and resultant cable temperatures materially exceeding the maximum values expected from survey data and calculations. This eliminates such fears as were expressed in a 1921 AIEE meeting[13] that "The operating man does not know where the hottest spot in his system is, and from the operating man's point of view, as I see it, I should prefer to keep the rating down to a reasonably conservative basis."

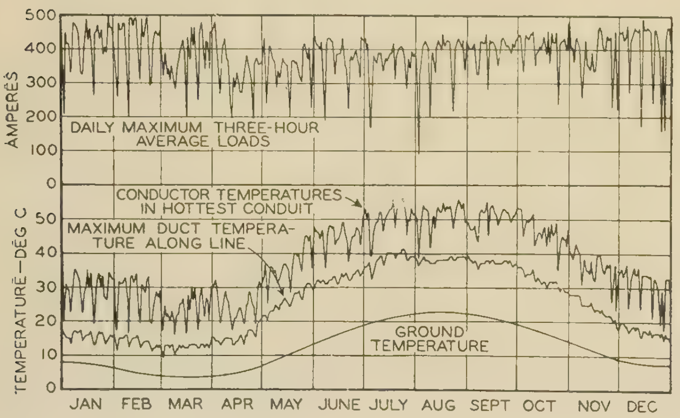


Figure 16. Load and temperature records for a 69-kv 750,000-circular-mil single-conductor solid-type cable

An inquiry just made by the author concerning the practice of 18 utilities indicates that most of them (having over 65 per cent of the cable in the country) closely follow the temperatures of conduits and cables where the temperatures are the highest, and 15 of them make some routine conduit temperature measurements.

Annual Temperature Surveys of System

In connection with operating the system or with proposed additions to it, the Commonwealth Edison Company makes routine and special surveys of conduit temperatures every year, thereby measuring 2,500 to 6,000 spot temperatures. These surveys yield considerable information also on water conditions. Most of these data are obtained in the summer, which is almost always the time of highest cable temperatures, even though the loads may be heavier in the winter.

The duct temperatures are obtained with mercury thermometers attached to a steel tape and inserted 20–25 feet into the apparently hottest empty duct. As illustrated in figure 15, the results of numerous detailed surveys of temperatures between manholes have indicated that temperatures taken 20 feet from the duct mouth are not influenced by manhole air. This is contrary to the opinion that temperatures taken 5 or 8 feet from the duct mouth are satisfactory and to another statement that 25 feet[14] is not far enough.

To obtain air temperatures in a duct containing cable, it is necessary that the cable fireproofing be broken away at the duct mouth and then later replaced. In addition, it appears difficult to get consistent results on the air temperature with a temperature-indicating device inserted between the cable and the duct, where the clearance is often only one-half inch.

The gas-filled bulbs of about 36 recording thermometers are installed approximately 25 feet into the hottest duct in the hottest location found in the annual surveys for each important line and installed also at other special locations. At any other location along the route of the line, the temperature may be estimated at any time during the year on the basis of records from these thermometers along with temperatures and other data obtained in the annual survey.

A record is kept for each location having a recording thermometer as indicated in figure 16. Another Chicago practice is

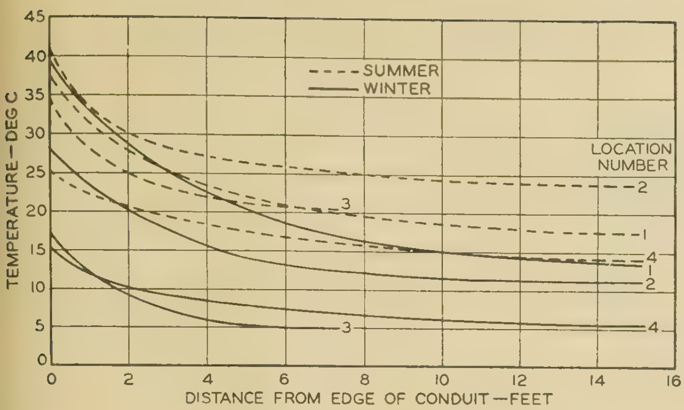


Figure 18. Temperature gradient laterally from conduits

to determine and record for each week the maximum load and the maximum three-hour average load for each line operating at nine kilovolts and over. Periodic load checks are made for lower-voltage cable.

Recording thermometers are installed at various depths in the soil at two locations remote from all other sources of heat for the purpose of establishing ambient earth temperatures. As indicated in figure 17, the annual variation in ground temperature becomes less with depth. The heating characteristics of a conduit section are based on the temperature rise of the air in that conduit above the ambient earth temperature corresponding to its depth.

Special Surveys

When large irregularities in the heating conditions along a section of conduit are indicated or suspected—such as an external source of heat being close to a portion of the conduit—then longitudinal temperature surveys are made. A 410-foot cable with thermocouples attached at 20-foot spacings is used. After this cable is installed in a selected empty duct, it may be moved to permit temperature readings at any desired spacing.

Usually the duct-temperature rise varies less than 10 per cent along the conduit section, except for the end 15 feet or so. Where one conduit crosses another, the temperature rise has been found to be as much as five or ten degrees centigrade in excess of normal. Another example of a special case occurred where a leaking steam main crossing the conduit created an excess temperature rise of about 50 degrees.

In 1926, thermocouples were installed in a plane at right angles through the conduit at ten different kinds of locations. At each location 20 thermocouples were placed in the periphery of the conduit and in the soil for 2 feet on each side of the top and bottom and for 15 feet each way from the sides. The principal findings are illustrated in the typical curves in figure 18. This study, together with other field observations, indicates that from 70 to 85 per cent of the total temperature drop from the hottest empty duct to the base earth temperature occurs from the side of the conduit and through soil, provided the width of the conduit is four ducts or less. Furthermore, separations, even though small, of adjacent conduits are found helpful. To make the heat effect of one conduit on an adjacent conduit negligible, the separation between conduits should be about twice the combined height of the two conduits, although separations over 15 feet are probably unnecessary. A slight advantage in heat conductivity was indicated for conduits using precast concrete ducts as compared to fiber ducts.

In 1936, thermocouples were installed transversely through a variety of conduits at four different locations mainly to aid in determining what value of thermal emissivity should be used for the sheaths in connection with calculating the rise of the sheath above empty-duct temperatures. Typical cross sections are shown in figure 19.

Although it had been appreciated that the temperature rises through conduits varied considerably with circumstances, the results obtained, as illustrated in figure 20, were surprising. If the empty-duct temperature is used as the ambient, then even for the cable in the hottest ducts it appears conservative, especially for the heavier loads, to employ the usual sheath emissivity constant of 1,200 degrees centigrade per watt per square centimeter. With reference to the air surrounding a cable in a duct, the sheath emissivity constant is usually 550 or less. These statements are based on the fact that for losses of four watts or more per foot of cable, most of the sheath temperature rises were below the lines drawn on figure 20 to correspond to the sheath emissivity constants of 1,200 and 550, respectively. Chicago data, together with other data, indicated also that the rise in temperature between the air in the occupied duct and the air in the hottest adjacent empty duct may be roughly, one degree centigrade per watt per foot of cable. Kirke[14] has indicated that the unit temperature rise of the sheath decreases as the cable losses (and sheath temperature) increase, and it appears that this phenomenon might well be taken into account for, particularly, very high loads, although it has not usually been done in Chicago.

The above tests showed that, contrary to the findings of some authors,[15,16] not all the heat comes to the ground surface during each season of the year. Usually in the summer the top row of ducts is hotter than the bottom row. The test results confirm the previous finding that the corner ducts are cooler by a small margin over other outside ducts.

Other Results of Surveys

Heating characteristics of conduits have been found to vary with the following conditions, excluding the effects of whatever neighboring structures may be present: type of soil, moisture content of soil, season of the year, size and material of duct, number and configuration of ducts, depth of conduit, and number of cables installed.[17,18] Considerable information has been published on the first two items. In Chicago, the temperature rise of the conduits in the soil having the poorest heat-dissipating characteristics is five times the rise in the best soil, all other conditions being the same, and

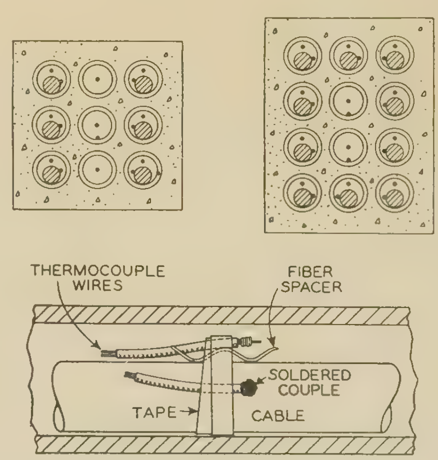


Figure 19. Installation of thermocouples in underground conduits

Thermocouples were placed in air or attached to sheath as indicated, and leads were imbedded in concrete of conduit and soil up to special terminals near street surface

excluding conduit under water. For the soils usually encountered, this ratio is 1.4 to 1. The conduit and soil dissipate the heat less readily in the summer than in the winter, the variation being from 1.3 to 1.

An increase in duct size increases the perimeter of the conduit and, thereby, decreases the unit thermal drop to base earth. Increasing the depth of the conduit may bring the conduit in contact with wetter soil,[19] but unless water is reached this is counterbalanced by increased length of the path of the heat flow to the ground surface.

As illustrated in figure 21, the duct temperature during any 24-hour period almost always varies only a few degrees, or less, and this variation is generally less than 15 per cent of the temperature rise of the duct above ambient earth, even though the cables have daily load factors of only 50 or 60 per cent. In view of such data, the conduit heating constants for determining the temperature rise for the hottest empty duct are calculated on the basis of the *average* heat loss during the 24-hour period. The maximum duct temperature usually occurs two hours after the maximum heat load.

Except for week ends or during emergencies, the heat generated does not vary considerably from day to day. In emergencies, correction must be made for the effect of the excess heat generated over the usual amount of heat; and it has been found that the temperature rise due to this excess reaches 40 per cent of its ultimate value in one day and 60 per cent in two days. These attainment factors are much lower and much higher than those given by some other investigators.

Instability of the heating constant has been found where the soil around the conduit was unusually dry and perhaps full of sizable voids, and where the duct temperature was somewhat above 50 degrees centigrade. An example of what may occur in such rare circumstances when the heating load in the conduit increases substantially above its usual value is illustrated in figure 22. In a few days the heating constant increased from 1.8 to 2.9 degrees centigrade per watt per foot of conduit. Even in this case the daily range was not more than 20 per cent of the duct-temperature rise.

In January 1921 a rare record of heating load of conduit and duct temperature was obtained on a short length of heavily-loaded conduit adjacent to a d-c substation. The conduit had 30 ducts containing 25 cables. The load on these cables was heavy for eight hours of each day, and light the remainder of the time. The maximum duct temperatures for the first five weekdays were, respectively, 80, 89, 94, 94, and 96 degrees centigrade with a daily variation of 20–25 degrees. The soil was a little better than average from a thermal standpoint. Since all cables involved were operating at 115 volts direct current, no cumulative cable heating resulted.

From studies of the two experiences cited, as well as considerations of soil characteristics in general, it appears that the maximum allowable duct temperature, if serious drying out of poor soil is to be avoided, is about 50 degrees centigrade. This limit agrees with Church's general recommendation.[20] For fair or average soil, this limit might be increased about 10 degrees; and for compact soil containing over 15 per cent moisture still further increases seem justifiable, although they are unlikely to be useful.

For a given conduit containing a given number of cables, the temperature surveys have indicated that the heating constant for a given time of year may vary considerably from the average in previous years. This is one outstanding

Figure 20. Temperature rise of sheaths above air in four conduits

All rises have been corrected to cable with $2^7/_8$-inch diameter when diameter was different. One type of point in graph for each location

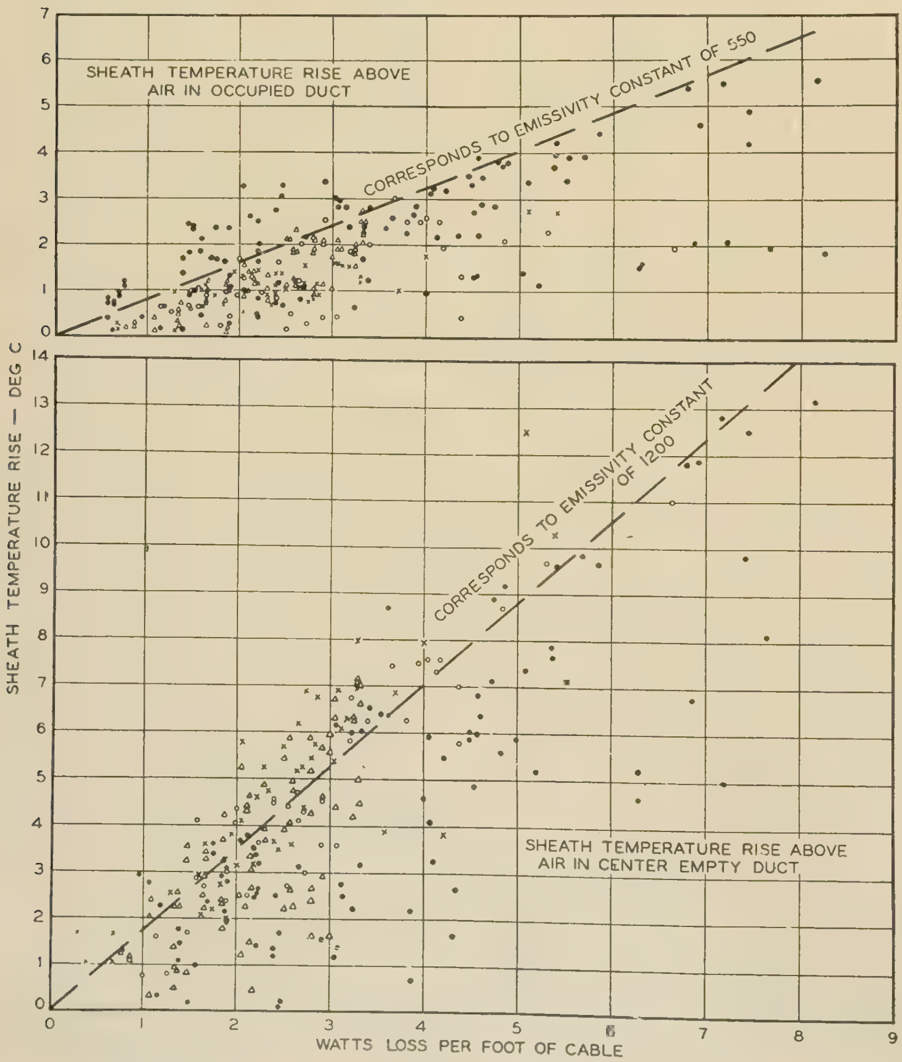


Table IX. Heating Constants for Underground Conduits

Number of Ducts	Heating Constant in Deg C per Average Watt of Loss Over 24 Hours per Foot of Conduit: Summer (June 1–October 31) Chicago	Summer NELA[21]	Summer Kirke[14]	Winter (November 1–May 31) Chicago
4	1.42	0.93	1.5	1.26
6	1.17	0.82	1.2	1.04
8	1.02	0.77	1.0	0.91
9	0.98	0.77	1.0	0.87
12	0.89	0.74	0.86	0.79
16	0.79	0.72	0.75	0.70
20	0.72		0.67	0.64
24	0.64		0.60	0.57

NOTE: Above is based on only the outside ducts being occupied with cables carrying load.

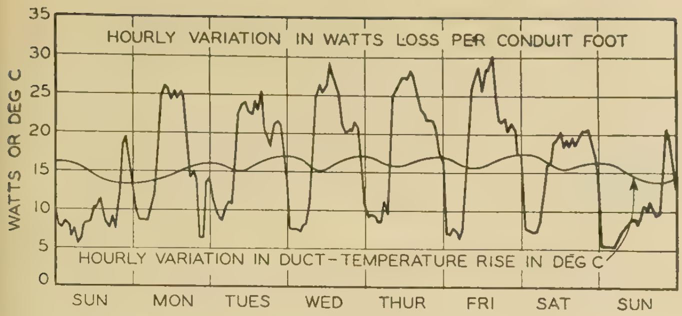


Figure 21. Heat and temperature variations in a conduit

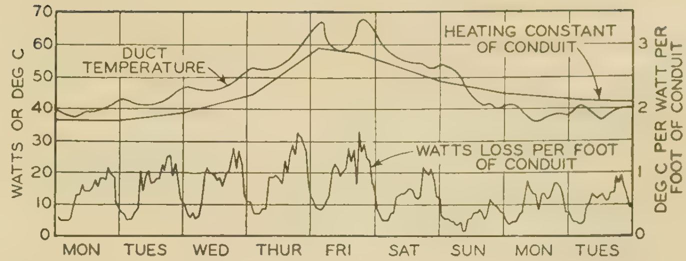


Figure 22. Temperature characteristics for a thermally unstable conduit

reason for making temperature surveys year after year. Detailed studies for 298 locations in Chicago showed variations ranging from 0 to about 50 per cent, the average being 20 per cent. The variations in percentage were less for the poor but fairly stable soils.

Heating constants for fair conditions with only outside ducts occupied are given in table IX. In Chicago, many center ducts are occupied by small cables used for relaying, signals, and voltage indications.

When data from temperature surveys and cable loads indicate that changes should be made in order to avoid excessive temperatures, the procedures successfully used are illustrated by the following:

1. When the problem requires an immediate solution, the conduit is usually flooded.

2. The soil around the conduit, especially when the heating characteristics are poor, may be replaced with bank-run sand and gravel. In such cases the replacement is for the soil from the surface to a depth in line with the bottom of the conduit and extending for about two feet on each side of the conduit. In one case, for instance, the result of this replacement for a 16-duct conduit was to reduce the heating constant from a range of 1.65–2.23 to 1.05–1.32. Since this conduit contained 12 single-conductor 13-kv cables which together served one side of a large transformer, it was not feasible to remove any of the cables. Even where it is feasible to remove cables, it has been found more economical in some cases to replace the soil instead.

3. One or more cables may be removed from the conduit.

4. The existing cable may be replaced with cable having a larger conductor.

5. A ventilated manhole may be installed where one conduit or a steam main crosses another conduit.

6. A steam main crossing a conduit may be specially insulated, and the separation perhaps increased.

In general, 9- or 12-duct conduits are the most economical in Chicago from an over-all standpoint, except where space is limited, in which case a greater number of ducts must be used. On the other hand, if it is known that the total number of cables will not exceed, say, four, then a 4-duct conduit should be built. On the Chicago system about 17 per cent of the total length of all ducts is in conduits having more than 12 ducts. The present trend in construction will reduce this percentage.

Table X. Maximum Allowable Temperatures for Some Chicago Cables

Kind of Cable			Maximum Copper Temperature Used (Deg C)	
Operating Voltage (Kilovolts)	Number of Conductors	Type	For Normal Rating	For Emergency Rating
0.12	Any	Solid	85	105
4	Any	Solid	85	105
9	3	Old rosin	81	95
9	3	Solid	81	95
12	3	Old rosin	77	90
12	1	Solid	80	100
12	3	Solid	77	90
66	1	Solid	58 or 60	60 or 65
132	1	Oil-filled	70	80

Notes: Three-conductor cable is of belted type; same cable of a given design is used for 9 and 12 kv. Temperatures for normal ratings are in accord with present rules. Above limits are used for the 132-kv cable already in service, because they give ample ratings for system requirements.

Part IV. Principles and Methods of Calculation

The method of calculating the ratings is in line with literature prepared by Simmons[21] and others. The chief principles and assumptions follow:

(*a*). Thermal resistivity of the insulation, the value of which is based mainly on Chicago tests, is taken as 550 degrees centigrade per watt per cubic centimeter for oil-filled cable, 600 for solid-type 69-kv cable, 650 for modern solid-type 5- to 35-kv cable, 800 for lower-voltage cable, and 900 for old rosin-impregnated cable.

(*b*). The thermal emissivity of the sheath is assumed to represent the thermal drop from the sheath to the air in the hottest unoccupied duct, and based partly on some test data is taken as 1,200 degrees centigrade per watt per square centimeter for cable with a diameter of three inches or more. For smaller cable the constant used is smaller, for example, 1,165 and 1,005 for two- and one-inch cables, respectively. For three cables in a duct, the equivalent surface is taken as 2.25 times the surface of one of the cables, while for two cables it is taken as 1.83 times the surface of one cable.

(*c*). In calculations of a-c resistance of cable, corrections are made for skin effect, copper proximity effect, and sheath losses. Skin effect is determined from Ewan's curves.[22] The copper proximity for multiple-conductor cable is usually assumed to be one-half the skin effect except for "compack" conductors, where it is considered as zero. The sheath losses in the three-conductor cable are calculated on the basis of Meyerhoff's[23] formula. For segmental single-conductor cable, the magnitude of the skin effect is assumed to be the same as for a conductor having a d-c resistance equal to 2.6 times the resistance of the conductor under consideration.

(*d*). The dielectric losses are determined from test data. For each type of cable a conservatively high dielectric loss-temperature curve is used. In determining the temperature rise of the conductor above sheath, it is assumed that the entire dielectric loss passes through one-half of the insulation resistance for single-conductor cable and through two-thirds of the insulation resistance for three-conductor cable.

(*e*). The cable dimensions are approximated for the cable as furnished. For instance, the sheath thickness is taken as being five per cent more than the nominal thickness, and the insulation thicknesses for 13-kv cable are taken as being six per cent more than the nominal thicknesses.

(*f*). In the calculation of general ratings, the heating constant is based on fair soil and on normal depth of conduit. In the determination of load ratings for special cases, investigation is made of conduit and ground conditions pertaining to each case to determine the specific heating constant applicable.

(*g*). Ground temperatures are taken as practically the highest that will occur during the two periods of the year, that is, 14 and 23 degrees centigrade for winter and summer, respectively, for conduits at normal depths.

(*h*). For general ratings, the first calculation, which is important, is based on the most prevalent size and on conservatively large conduit sections.

Table XI. Load Ratings for Typical Cables in Chicago

Cable			Rating in Amperes			
			Normal		Emergency	
Normal Size (Circular Mils)	Number of Conductors	Operating Kilovolts	Summer	Winter	Summer	Winter
1,500,000	1	0.12	1,230	1,330	1,460	1,520
375,000	3	4	340	375	400	425
250,000	3	9	230	255	260	285
500,000	3	12	365	410	420	465
350,000	3	33	315	375	340	400
750,000	1	66	460	480	510	550
1,100,000	1	132	450	550	735	825

NOTES: Ratings are for the maximum three-hour average load during a day. Single-conductor cable has no sheath losses. The normal ratings for the example of 132-kv cable are limited by thermal conditions created by other cable in the same conduit.

(*i*). The characteristics of the loading of the cable as to daily load factor, heating load factor (ratio of average loss during 24 hours to loss corresponding to average load over the three-hour maximum period), and ratio of usual daily maximum three-hour average load to normal rating are based on present and expected future trend. This three-hour average load is usually five or eight per cent below the very maximum load during a day and gives a good practical value to use in determining temperature rise of a cable. The heating load factor is usually taken, for instance, as 60 per cent for 13-kv three-conductor cable and as 70 per cent for 69-kv single-conductor cable; and the ratio of daily maximum three-hour average load to normal rating is taken as 68 and 90 per cent for the two cables, respectively. An example of an exception is for nine single-conductor cables feeding the 12-kv side of a large transformer; then the calculations are based on all nine cables having maximum loads equal to the normal or emergency ratings.

(*j*). The maximum allowable copper temperatures used at present in Chicago, as indicated in table X, are, in general, less than recognized as safe in part I of this paper. It is considered wise to be conservative on this matter until additional test data and, more particularly, operating experience are available, especially because of the probability of an excessive number of sheath cracks incidental to extra-high copper temperatures. In some instances, the ratings actually used produce lower maximum copper temperatures than given in table X, because of any of the following limitations: (1) allowable maximum duct temperature, depending on the type of soil; (2) allowable maximum duct temperature, depending on other installed cable that may be predominating in importance over the cable under calculation; (3) allowable daily range in temperature for heavy solid-type insulation.

(*k*). The cable movement at the duct mouth is determined mainly from the standpoint of the usual expected daily maximum load. The accepted maximum allowable movement for the usual daily loading is 0.5–0.75 inch at a duct mouth, the lower maximum applying for the more important cable. The cable movement is given a little consideration in connection with the emergency rating.

Ratings are usually determined for two periods of the year, summer and winter. For the important 66-kv tie-lines where the desired loading is frequently great and the load may be controlled, it has been found advantageous to give a special set of ratings applying for the periods of June 1–July 15 and October 1–31 in order that during these periods the ratings may be above the midsummer ratings. For each period of the year, calculations are made for all kinds of cable for the normal ratings and for the emergency ratings. The emergencies are considered to last one and two days, respectively, for solid-type and oil-filled cable because experience shows that repairs may be made to a circuit within those periods. In addition, special ratings are occasionally calculated where requests are made for larger than the general ratings and it is found that the lines are installed under abnormally favorable conditions, or where the surveys of the group following temperatures show the ratings should be less than the general ratings on account of subnormal local thermal conditions. Another special set of ratings is given for some tie-lines where the emergency will exist for only one or two hours, with the result that the temperature rise of the cable and conduit during the emergency is materially less than would be the case for protracted operation. The transient heating characteristics of the cable in such cases are determined on the basis of data by Miller and Wollaston.[24]

The common size outside the downtown area in Chicago is the 500,000-circular-mil three-conductor 13-kv cable operating at 12 kv. It is assumed in the first calculation that there are eight such cables in a 12-duct conduit, and the normal ratings are determined on the basis that seven of the cables have a usual daily maximum load of 68 per cent of the normal rating and that the eighth cable is carrying the rated load. The emergency ratings are then calculated on the basis that seven of the cables are carrying the calculated usual load and the eighth cable is carrying the emergency load. In determining the ratings of cable operating at, say, 4 kv, it is assumed that there are five of the 12-kv circuits in the conduit and three of the 4-kv cables, and that the 12-kv circuits and two of the 4-kv cables are carrying the usual loads.

Where the conduits are abnormally warm or congested, as at generating stations, where 12- or 16-duct conduits may be less than ten feet apart or may cross in some cases, it is the practice to specify an extra-large size of conductor in order that the portion of circuits in such conduits shall not limit the carrying capacity of the circuits as a whole. For example 650,000-circular-mil is used in place of the usual 500,000-circular-mil conductor on the three-conductor lines operating at 12 kv; and 1,000,000-circular-mil is used in place of the 750,000-circular-mil conductor on the single-conductor lines operating at 66 kv. The use of the extra copper for a short part of a line has been found to be an extremely economical procedure.

The general ratings are given for some typical cable sizes in table XI.

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Discussion

G. B. Shanklin: See discussion, page 560.

Wm. A. Del Mar: See discussion, page 561.

L. I. Komives (nonmember; The Detroit Edison Company, Detroit, Mich.): Mr. Halperin's findings were very interesting in regard to the beneficial effect of increased offsets in manholes versus manhole lengths. What other means have been found in Chicago to prolong the life of the lead sheath by changing conditions in the manhole outside of lengthening it or broadening it?

Under the heading "Summary," in part I of the paper, a remark is made as follows: In general, this effect (power factor increased at normal operating temperatures) should not be expected to shorten the life of the cable adjacent to joints. It seems that in the experience of The Detroit Edison Company the presence of joint-filling compounds of petroleum base, because they mix readily with the cable impregnating compounds, results in a considerably higher power factor.

E. W. Davis (Simplex Wire and Cable Company, Cambridge, Mass.): Careful field records and research data are of infinite value to the cable manufacturer. No product made is first presented in its perfect state even for services in which conditions are static; and for utility distribution systems which continually change in practice, economics, and operating conditions, it is even more evident that continual change and improvement is essential in its component parts. Such work as here presented which sums up manufacturers' results and technique, field experience, and economic distribution practice definitely points the way to further work by manufacturers to develop more satisfactory cable for present day practices.

The conclusions drawn by the author are general. With a problem containing so many variables probably no general conclusion is safely applicable to all cases and bears out the contention of cable engineers which is of many years standing that current ratings by manufacturers must be approximate and then considerably on the safe side.

Undoubtedly some increase in operating temperature could be allowed based on the probable performance of modern cable insulation only. However, such increase is counteracted by detrimental features concerned mainly with the performance of the lead sheaths such as expansion from pressures, cracking, electrolysis, and also probable changes in heating constants of the ducts and surroundings due to drying out. It is interesting to note that in the main all detrimental features are due to varying conditions imposed by load cycles.

More definite practices and rules governing permissible overloads have been greatly needed for many years. Here again it is probably not practical to use a general rule but allowable overload temperatures and their durations could well be developed for various insulations. The present AIEE rule for maximum continuous operating temperature is not complete enough for modern uses. The author's tests and summation approaches this subject.

The question of thermal stability of old rosin cables is interesting. We know of very old rosin-rosin oil cables of high loss still operating at 12 kv after many years. Thermal stability at this voltage is not appreciably affected by losses in spite of the great agitation on this subject a few years ago.

The opinion given that admixing of joint compound with cable compound is not detrimental is interesting and also directly opposite to the ideas of some other utility engineers. We are inclined to agree with the present writers in this respect for moderate voltage cables.

Part II of the paper dealing with sheaths is quite unique and well done, particularly that part dealing with equations for expansion of cable. We would not expect the coefficient of a cable to be as near to that for a solid copper bar as here taken. The lay of cabling and stranding must have some effect. Considering a stranded single-conductor cable in which the strands are laid at an angle of 30 degrees, an increase in the length of the strand is not directly applied to the length of the cable as shown below:

Let

l = lay or pitch of strand along cable
d = pitch diameter of layer
L = length of strand

$$= \sqrt{l^2 + (\pi d)^2}$$

Let L increase by ΔL and assume no change in pitch diameter

$$L + \Delta L = \sqrt{(l + \Delta l)^2 + (\pi d)^2}$$

or $l = L \cos 30°$

It would be expected that

$$\Delta l - \Delta L \times \cos 30° = 0.866\,\Delta L$$

and that

$$C \text{ effective} = 16.7 \times 10^{-6} \times 0.866$$
$$= 14.5 \times 10^{-6} \text{ for the assumed case}$$

This effective coefficient would be different for multiconductor cable and different again where tension or pressure existed since there is no reason why either could not change the angle at which the longitudinal force of expansion and contraction is acting.

With reference to sheath life we find the probable life as determined by test very interesting. Many improvements in lead-sheath quality and workmanship have been made quite recently and undoubtedly more will follow. Thicker lead sheaths promise some help but alloyed sheaths may give equal or better results more economically.

Robert J. Wiseman (The Okonite-Callender Cable Company, Passaic, N. J.): Mr. Halperin has written a very remarkable paper, describing the researches his company has been conducting for many years and now have reached a point where the story may be told. I think the title might be changed to something like "Why Cables Operate as They Do" for he gives a very fine exposition of the problems of operation and how they might be solved. He is helping all of us better to appreciate the utility's operating problem and we cable manufacturers will have to give considerable study to his paper and in fact all of the papers dealing with cable operation in order to find how we can help to improve their operation or increase their life. Therefore, our attention is brought to the statement in the third paragraph under Part I in which he states that the main consideration is the influence of heating on the ability of the cable to withstand removal and reinstallation without material lowering of the dielectric strength of the cable. I think that all of us will agree that if we could keep a cable at a constant temperature continuously we would be willing to agree to higher temperatures than are standard at the present time, but it is the upsetting effects of the high and low temperatures that the cable is subject to that causes a cable to lose its physical as well as electrical properties. Its life is shortened and, therefore, we must try to hold down the maximum temperature in order to get as far as we can guess a life expectancy that is in fair agreement with other kinds of equipment. As Mr. Halperin states, there is a scrapping of a cable before its useful life is reached because of causes other than insulation deterioration and one of these is that the cables have not been operated in most cases to their present permissible temperature. Until we do get this experience it is difficult for us to accept higher permissible temperatures than now set up. As this experience can only come from the utilities we must await their collecting the information for us.

This is equally true of the emergency rating of cables. We don't know how much a cable will lose in years of life by short periods of overloading. It has not been possible to determine it because the last 12

to 15 years have seen a remarkable improvement in the quality of oil and paper as well as manufacturing methods. If the MIT research were conducted today it is possible that we would find less deterioration than in 1926 when it was made. If it were possible to follow closely the daily load cycle on a cable and its history for several days previously, a method for determining an emergency rating could be set up that would still keep the maximum conductor temperature close to the permissible values. It would be a tedious job and require almost continuous calculating of the conductor temperature as the load varied. It would not be practical. A comparison of transformers and cables in their emergency ratings is not entirely possible. The former has plenty of oil which can circulate and assist in cooling and also a large volume of material for heat absorption and if moved, it is done as a unit without upsetting the insulation. For cables we have small volume, no oil circulation to assist cooling, and the cables during their expected life will be moved. The aging of transformers due to heating has been well studied from the life expectancy viewpoint and, therefore, the limits set up are probably based on this knowledge. Why should cables be set as high as transformers in the light of the difference in their make-up and operation?

Mr. Halperin's description of his aging tests on 12-kv cable with pressure-tight potheads and open potheads checks our experiences on aging tests on 69-kv solid-type cable. When we sealed up the end of the cable and conductor to prevent oil flow from the pothead into the cable we obtained higher pressures and higher vacuum in the cable than when the ends were left free to permit the flow of oil back and forth and obtained a longer life on cables with ends open. We then studied the effect of more freedom of flow of oil between a joint and a cable by eliminating the varnished cambric wrapping and substituting tubes and open-mesh cotton tapes directly over the connector. We got better stability, lower pressures, and longer life. We have suggested to several utility engineers that better operation would be obtained if this were done and I hope some will do so soon. This will eliminate the experience described by Mr. Halperin on joints in part I. I cannot appreciate how using low-loss varnished cambric taping is going to improve the mechanical problems of his joints.

Part II dealing with limitations due to the sheath is very instructive. Mr. Halperin's experience that increasing the lead thickness $^1/_{64}$ inch cuts the expansion in half is what we found years ago and I have reported it at other meetings. We found this to be true when comparing $^8/_{64}$ inch to $^9/_{64}$ inch, and $^9/_{64}$ inch to $^{10}/_{64}$ inch. It is one of the reasons that I advocated years ago increasing the wall thickness of our large-diameter cables in order to reduce the radial expansion. I still believe in it.

For single-conductor cables the oval-shaped conductor with a circular sheath is the solution of the internal-pressure problem. This has been proved in tests by our associate company in England, The Callender's Cable and Construction Company, and in our laboratories. On new cable we did not exceed an internal pressure at the sheath of 20 pounds per square inch and a vacuum higher than 5 inches, whereas on circular conductors we have gotten 85 pounds per square inch pressure and 20 inches vacuum. We have obtained about 25 per cent longer life on accelerated aging tests using oval conductors. An oval conductor and free-flowing joint will give a much longer life in single-conductor solid cables than obtainable with round conductors and solid joints.

I wish to congratulate Mr. Halperin on the section dealing with the manner in which a cable moves with changes in temperature. It is understandable and sounds logical and makes us glad to know that cable movement is not as much as we thought and how fortunate we are. If it did move in the amount a solid copper rod expands, the utilities would be experiencing a greater number of lead failures and replacements which is costly, or would have to build much wider ducts which is also costly. I would like to ask one question. How much actual influence will the size of the cable compared to the size of the duct have? The restraining forces in the duct for a cable close to the size of duct will be higher than for a smaller cable in the same duct, but the chance of snaking or flexing in the duct is less in the former. Do they offset each other so that we can say that all cables act alike in the same duct?

We have been told by utility engineers of the cracking of lead sheaths near the duct mouth due to cable movement and have been studying ways of overcoming it. We have considered cutting the sheath and inserting a bellows which relieves the lead and permits cable movement at one location. One of the companies had the same idea.

Part III, dealing with the heating characteristics, brings forth few questions to discuss as it is well written and quite detailed. Under "Annual Temperature Surveys of System," fifth paragraph, reference is made to using the maximum three-hour average load in determining the load factor. Although this is usually done, we have wondered sometimes on what basis a three-hour time period has been taken instead of a one-hour period.

Under "Special Surveys," fifth paragraph, is not the low surface emissivity constant of 550 likely to be due to inaccuracy in measuring the air temperature, the latter probably being too high which would give a low temperature gradient? We consider temperature measurements in an empty duct more accurate. The temperature of the duct wall for a duct containing cable is probably not more than one or two degrees higher than the temperature in the empty duct due to heat conduction of the concrete.

Part IV, item *b*—we understand the thermal emissivity of a sheath is assumed to represent the thermal drop from the sheath to the air in the same duct and not to the air in the hottest unoccupied duct. The thermal duct constant takes care of the duct and the surrounding earth. We like the method proposed by Kirke, and which is used in England, to use a separate term for the duct structure and another for the earth. If this is done, the earth term can vary according to the kind of soil. We also hope that some day a study of these constants for a small number of ducts in a duct bank and very few cables installed will be determined experimentally so that we will be more sure of what they are than we are today.

E. R. Thomas (Consolidated Edison Company of New York, Inc., New York): Mr. Halperin is to be complimented on the scope and treatment of the various factors influencing the load ratings of cable. In part II the treatment of cable movement presents a fresh theoretical explanation of the magnitude of movement which will probably be encountered and the wealth of field test data seems to confirm the theory advanced.

In connection with induced sheath potentials on single-conductor cables treated in this section of the paper I would like to point out that Searing and Kirke in an article "Reduction of Sheath Losses in Single-Conductor Cable," *Electrical World*, October 6, 1928, suggested the desirability of limiting the voltage between cable sheaths to a value of 200 volts during fault conditions. This limit was set as one dictated by safety to personnel who might be working in manholes containing these cables during the period when a fault occurred. It was assumed that high-voltage circuits in general had impedances in the form of transformers or protective reactors which would limit the fault currents to approximately ten times the full-load currents and thus it was expected that the voltage between sheaths under normal load conditions would be about 20 volts and the voltage to ground about 12 volts. Tests were carried out on lead electrodes and short lengths of cable sheath and the data were reported showing the loss in weight which might be expected for various current densities of alternating current. From this data it was the conclusion that this arbitrary value of 20 volts between sheaths set up from safety considerations was a value of potential so low that the expected sheath life of cable due to a-c electrolysis would far exceed the expected life of the cable due to other causes even when that cable was installed and operated in the presence of high-conductivity tidewater.

We have not experienced any failures due to a-c electrolysis during the period of years which we have operated single-conductor cable installed with cross-bonds as described in the article by Searing and Kirke. Some of these cables normally operate at voltages to ground in excess of 15 volts and are submerged in tidewater. I do not feel that voltage values of even 20 volts to ground would be a factor detrimental to the life of the cable due to any a-c electrolysis which might result even if the cables were operated submerged in high-conductivity water. If one considers the possible shock hazard involved it would seem quite likely that considerably higher differences of potential may be experienced on a system using sheath-bonding transformers.

L. F. Roehmann (Hastings-on-Hudson, N. Y.): I want to comment on Mr. Halperin's paper, on the section dealing with the cable movement. The cable engineers should be thankful to Mr. Halperin and his collaborators that they considered, for the first time, the restraining and frictional forces in studying cable movement due to

temperature changes. The conditions are similar to those occurring in the rails of a railroad track which are also subjected to movement and to internal stress. In the cable technique these problems are, of course, of lesser importance than in railway engineering, but I am glad that they have been broached.

If one tries to check the formulas presented in Mr. Halperin's paper, one has some difficulties. Therefore I would appreciate it if Mr. Halperin would indicate, perhaps in an appendix, how and under which assumptions these formulas are derived, inasmuch as they form the base for the subsequent discussion of the experimental results.

Herman Halperin: The submitted discussion on this paper, as well as other discussions that have been given me, is greatly appreciated.

Some of the discussers apparently have missed one of the main points of the paper, which is that emergency ratings have become feasible, not because of mere conjecture, but because of a thorough engineering study which brought out the big increase in the quality of installed lines during the past 15 years, the moderate temperatures at which cables are usually operated, the indications of little or no deterioration in tests of various kinds of cable and of paper insulation by itself at temperatures much above those now allowed by the existing rules for cables, etc. Although it may have been wise 18 years ago (time of presentation of similar papers) to keep temperatures always below the limits given by the AIEE rules, it does not seem wise to do so now in view of the various large improvements since made by manufacturers and by utilities.

As is recognized in the paper, the use of the emergency ratings may result in moderate increases in maintenance and in failures, but even with these increases these items will be small as compared with what prevailed 15 years ago. The tendency for these items to increase will, of course, be counterbalanced by further improvements.

Another point that pertains particularly to the solid-type cables is the fact that emergency ratings based on temperatures greatly in excess of the temperatures permitted by existing rules are feasible provided the usual daily loading is moderate. If the daily loading is heavy, with the result that the cable operates at temperatures very close to the limits now allowed by the AIEE rules, and furthermore if the loading varies considerably during each 24 hours so as to result in large daily temperature ranges, then the margin of safety in the installed underground circuit is decreased. This means that the safe temperature for emergency operation becomes less than would be the case for moderate loading.

Wiseman points out that the operating companies have not collected much information on the operation of cable up to the present permissible temperatures. Of course, this is due to the fact that the permissible temperatures were applied to the rare emergency conditions, which resulted in building cable systems to operate normally at very low temperatures. In order to operate the cables nearer to the present permissible temperatures, it is necessary to permit the temperatures to exceed these limits during rare emergencies.

Apparently some discussers have not taken full cognizance of the indications, as they seem to my associates and me, that somewhat *higher* temperatures than given in the recommendations in the paper are safe for the insulation for emergency use according to the results of the *tests* and *field investigations* made in Chicago and elsewhere. Of course, as indicated in the paper, the critical limiting factor may be the sheath during the usual daily loading which corresponds to the increased maximum allowable loading.

Shanklin refers to the fact that the maximum allowable copper temperatures in Europe are lower than set by the American rules. Our studies of English and continental literature, as well as private information, do not disclose adequate technical explanation for these differences in allowable temperatures. In the successful tests on single-conductor cable at 150 kv to ground at Arnhem, Holland, the temperatures have been usually above the limits allowed by rules either in the United States or in Europe. Furthermore, it is of interest to note that when it comes to other equipment the maximum allowable temperatures in Europe are comparable to those allowed in the United States; and, in that connection, one frequently sees figures such as 100 or 110 degrees centigrade, which is much above the limiting temperatures set for cables in Europe.

It is of interest to note that Shanklin sees no objection to "the use of emergency overload ratings under favorable conditions and where performance can be watched closely." In the paper it was intended to adjust the emergency overload ratings to the field conditions as well as to limitations imposed by the insulation or sheath or both. If the manholes, for example, are small, then the effect of this condition must be taken into account.

Roehmann has requested the derivation of the formulas relating to cable movement. His interest, as well as the interest of others, in this general subject is gratifying, but nevertheless it seems desirable to omit taking the space to present this derivation because of the unusual amount of space already taken by the paper. I shall, however, be glad to send a copy of the derivation to anyone upon request.

Davis' objection to using the same coefficient of thermal expansion for cable as for bar copper (16.7×10^{-6}) seems to be based on erroneous assumptions. He derives a value of 14.5×10^{-6} on the assumptions (1) that the angle of lay of the strands in a conductor is 30 degrees and (2) that the expansion of the conductor is to be found by multiplying the *total length of the strand* by the expansion coefficient. The usual angle of lay of strands is nearer 12 degrees than 30 degrees, which would give a value of 16.34×10^{-6} for the coefficient when calculated by his method. This is only two per cent less than the value used in the paper. The second assumption is not in accordance with the usual practice of multiplying the axial length of the cable by the coefficient to get the expansion. The value given in the paper is correct when used in connection with the axial length of the cable, instead of the total length of the strand.

Regarding the discussion by Thomas as to the limiting induced sheath potential for single-conductor cables, the maximum allowable values set in Chicago were based on extensive laboratory and field tests, as indicated in reference 12 of the paper. For instance, two trial installations in the field were operated at 18 volts to ground in one case and 20 volts in the other case for $1^1/_2$–2 years, and corrosion was found in both cases. It therefore seemed to us from these and other data that normally the sheath potentials to ground should be considerably below 18 volts in order to avoid a-c electrolytic corrosion over a period of many years. It is recognized that in some instances such potentials may be safe, but once corrosion starts, mitigating measures may not stop it entirely. We have, therefore, set a limit of 11 volts to ground for usual daily loading, and periodic examinations of cable under water in manholes have indicated this limit to be reasonable.

When it comes to the personal safety angle in connection with sheath potentials, it should be recognized that the transient sheath voltages may be a matter of several kilovolts instead of just the 200 volts at generated frequency mentioned by Thomas. This fact, plus the fact that there is no considerable difference in the order of magnitude of the transient sheath potentials between bonding systems using the cross-bonding scheme or open-end bonding or sheath-bonding transformers, is given in the paper "Transient Voltages on Bonded Cable Sheaths" in the January 1935 issue of ELECTRICAL ENGINEERING. These transient potentials cause no harm to individuals nor to sheaths, and experience has demonstrated that they will cause no harm to insulators or other accessories provided the presence of the transients is recognized and adequate insulation is provided to withstand them.

Replying to Komives, careful training of cable in manholes has been quite effective in prolonging sheath life. We have standards set up for all types of manholes (straight, *X*, *T*, etc.) and all numbers of ducts. These are closely followed. The desired joint position is accurately indicated on the cable installation print for each job. In the case of manholes not conforming to standard, the cable training is worked out with the aid of a scale model of the manhole.

Regarding the effect of nonfluid joint compounds on the power factor of cable, we get high power factors in solid-type cable adjacent to joints filled with asphaltum-type compounds, but we have never had a failure ascribed to this cause. Nevertheless, as a matter of improvement, we are continuing to give consideration to insoluble compounds.

It has been found also in Chicago that thin joint oil entering solid-type cable has caused increased power factor of the cable insulation, due mainly to contamination of the oil by the varnish of the old-type varnished cambric used in existing joints. This action occurs essentially in manholes where the ambient temperatures are lower than in the ducts and, except for a few special cases, this cable will remain thermally stable to the highest temperatures mentioned in this paper.

The use of the taped joint decreased the possibility of bulging and collapsing of three-conductor joints by permitting the

Maximum Safe Operating Temperature for 15-Kv Paper-Insulated Cables

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THE capital investment in the underground cable system of any large metropolitan utility is so great that any means of increasing its usefulness is of great and immediate interest. The operation of paper-insulated cables at higher copper temperatures than are accepted as normal, offers one obvious means of increasing the usefulness of the cable system where voltage regulation does not limit load.

Previous reports on thermal limits of paper insulation have been presented by Del Mar,[2] Torchio,[3] Roper,[4] Clark,[5] Fisher and Atkinson,[6] and Bush.[7] Electrical and physical characteristics of impregnated paper as affected by temperature were investigated at the Massachusetts Institute of Technology under the direction of a joint committee on research set up by the National Electric Light Association and the Association of Edison Illuminating Companies. This work was reported from time to time over the period between 1921 and 1929.

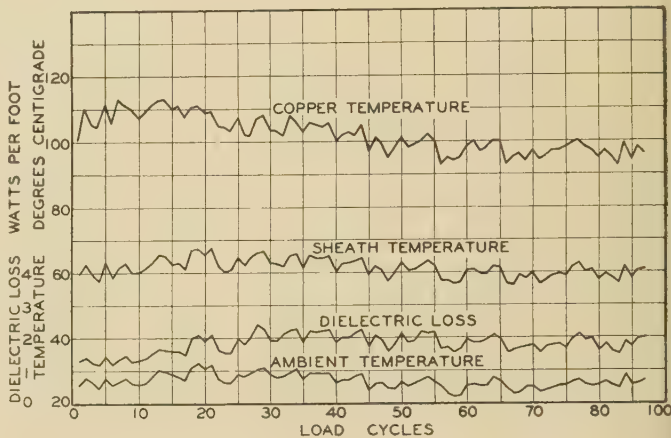

Figure 1. Load-test measurements at three phase, 13.6 kv, 60 cycles

Three - conductor 250,000-circular-mil 15-kv cable

About five years ago, the Edison Company in New York started a series of laboratory and field tests to learn the effect of intermittent operation of paper-insulated cables of the 15-kv class at temperatures above the level generally accepted as maximum for their class. This we believe is the only extensive field investigation of the kind that has been made on cable as a unit instead of on constituent parts of the cable and, for that reason, may be of interest.

Laboratory Test

Investigation was begun in January 1934 with laboratory tests on five 50-foot lengths of cable withdrawn from service. The cable was three-conductor round 250,000 - circular - mil paper - insulated belted - construction lead - covered cable which had been in service on the system for about 25 years. For about half of its life, prior to these tests, it was operated at 6.6 kv, 25 cycles, and thereafter at 11 kv, 25 cycles. The cable has rosin-compound-impregnated Manila-paper insulation, 170 mils conductor insulation, 155 mils belt insulation, jute fillers, and 125 mils lead. Examination of samples prior to the testing showed the paper tapes to be wide and badly wrinkled, dry, lifeless, and covered with powdered rosin. A few of the sections examined, however, showed the paper tapes to be fairly well saturated

The laboratory test connections were arranged so that load current could be circulated in each phase conductor and a three-phase test potential of 13.4 kv, 60 cycles, applied continuously. The load currents were applied for a period of eight hours during each working day and then removed for the remaining period. The values of the current were adjusted to obtain different maximum copper temperatures.

The potheads used on the first sample of cable to be tested were filled with petrolatum. Some of the potheads used were not of an oil-tight type and compound was forced out during the loading cycles. Potheads on the second sample were filled with paraffin in order to see what effect this compound might have

Paper number 39-70, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 25, 1938; made available for preprinting January 3, 1939.

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The authors gratefully acknowledge the co-operation of the many individuals in the operating, research, test, and engineering divisions of the company who collectively made possible the carrying on of this study.

1. For all numbered references, see list at end of paper.

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use of a substantially smaller diameter of sleeve than was feasible with the cell-type joints formerly used.

We do not know exactly what effect the ratio of cable diameter to duct size has on cable movement at the manhole. Our data do not show that there is any definite correlation. We would expect less movement in larger ducts. It is difficult to get significant data on this point because we do not have the larger "stiff" cables in very large ducts which could be studied for comparison with similar cable in normal-sized ducts. We do have cables that are small compared with the duct size, such as 4/0 four-conductor four-kv cable in $3^1/_2$-inch duct, but such cable is relatively flexible, and the ratio of copper cross-section to total weight is low.

Our studies on cable movement and related problems are continuing.

Wiseman, in referring to part III of the paper, inquires as to why the maximum three-hour average load is used in determining the load factor. Step-by-step calculations have been made in the past to determine the maximum temperature based on readings of the current taken every half hour, and it has been found that the resultant temperature is very close to what is obtained by using for Chicago conditions the simple arithmetical average load over the peak three-hour period.

He refers to the possibility of inaccuracy in taking the air temperatures in connection with figures 19 and 20. It may be noted that considerable care was exercised in taking these readings. In fact, the care was greater than may be expected from a field crew when taking temperatures of occupied ducts by the usual methods. As to his further remarks on thermal emissivity, we have found it reasonably correct (see figure 20) to use an emissivity constant approaching 1,200 and have the calculated thermal drop represent the thermal drop from the sheath to the air in the adjacent warmest unoccupied duct.

Table I. Classification of Troubles, Long-Time Test on Three-Conductor 250,000-Circular-Mil Cable

Cause of Trouble	During Test Number	During Test Per Cent	After Test Number	After Test Per Cent	Total Number	Total Per Cent
Cable						
Dielectric failure	16	54	2	17	18	43
Lead-sheath failure	7	23	6	50	13	31
Miscellaneous	1	3	2	17	3	7
Unknown	1	3	1	8	2	5
Joints						
All	5	17	1	8	6	14
Total	30	100	12	100	42	100

on the cable under test because some of the joints on this cable in the field were filled with paraffin. The potheads installed on the remaining three samples were filled with an oil-insoluble hard compound. This prevented the migration of compound into or from the cable during loading cycles. At the end of each loading cycle dielectric power loss, voltage, charging current, and sheath and ambient temperatures were measured.

The curve shown in figure 1 is typical of the values measured on the test samples. The sample for which data are shown in figure 2 failed before the end of the 16th load cycle. An attempt was being made on this sample to operate it at a copper temperature of 140 degrees centigrade. This temperature was at the borderline of thermal instability and slight changes in the load current resulted in a runaway condition and failure.

Examination of this cable at the end of the tests showed severe carbonization. The tapes near the conductor were reduced almost to ash for the entire length of the sample. The cables used for the other tests showed signs of carbonization in each case where the temperature had exceeded 130 degrees centigrade to any extent during the test. One sample whose temperature varied between 120 degrees and 130 degrees centigrade showed no signs of carbonization. The only sample whose temperature never exceeded 115 degrees centigrade also showed no signs of carbonization. At the end of the test, the paper tapes near the conductor on this sample seemed somewhat drier and more brittle than at the beginning of the test. This was the only noticeable change.

Table II. Failure Rates by Manufacturers, Long-Time Test on Three-Conductor 250,000-Circular-Mil Cable

Manufacturer	Sections	Section-Years During Test	Failures per 100 Section-Years During Test	Failures per 100 Section-Years After Test
A	49	95	5.3	4.5
B	22	27	3.7	0
C	22	36	39.0	6.3
Other	21	21	0	5.0
Unknown	15	20	25.0	20.0

The conclusions drawn from these tests were that the practical limit for safe maximum operating temperatures in the field should be 100 degrees centigrade. It was also concluded that if any failures were to occur as a result of cumulative heating from high dielectric loss, they would occur within the first few load cycles, since the trend of dielectric loss showed little tendency to change after the first few test load cycles.

Long-Time Field Tests on Old-Type Cable

The conclusions based on four months of laboratory operation could be verified only by similar tests carried out under field conditions over a longer period of time. Four 11-kv 25-cycle feeders were selected for the field test. A temperature survey was made along the routes of these feeders in order to determine the existing empty-duct temperatures. These values ranged from 30 to 40 degrees centigrade. Based on this information, a value of current was calculated which would result in a 100-degree-centigrade copper temperature for this cable having dielectric loss characteristics as determined from laboratory tests and for an assumed average empty-duct temperature of 35 degrees centigrade. The computed value of current was 290 amperes and this was the assigned value of loading to be carried on the cable in these feeders. The field tests on the four feeders started in July 1934. These feeders contain a total of about $6^1/_2$ miles of old 250,000-circular-mil cable.

The feeders under test supplied d-c substations from a generating station. Daily load cycles of 290 amperes on each feeder over an eight-hour period were obtained by using substation tie feeders to transfer load to and control load on the feeders under test. The test loads were not applied on week ends or holidays, and there were a few short intervals when operating conditions necessitated the omission of the test loads. The week-end periods were utilized to make kenotron tests at 20 kv to ground for five minutes in order to determine whether the insulation resistance changed as indicated by the milliampere leakage current during the test. The values of current measured during these tests show very little change and could probably be accounted for due to variations in temperature. It was further felt that the test might serve a useful purpose in developing incipient failures and thereby avoid some operating failures.

Due to the variations in duct constants and heating from other underground structures, the actual copper temperatures which resulted had maximum values ranging from 78 degrees centigrade to 133 degrees centigrade as determined from calculations based on measured empty duct temperatures obtained at various locations during the testing period.

The last of the original four long-time test feeders containing the three-conductor 250,000-circular-mil cable was taken off test in February 1937, about $2^1/_2$ years after the test began. The other three feeders had been on test for 1, $1^1/_2$, and 2 years, respectively.

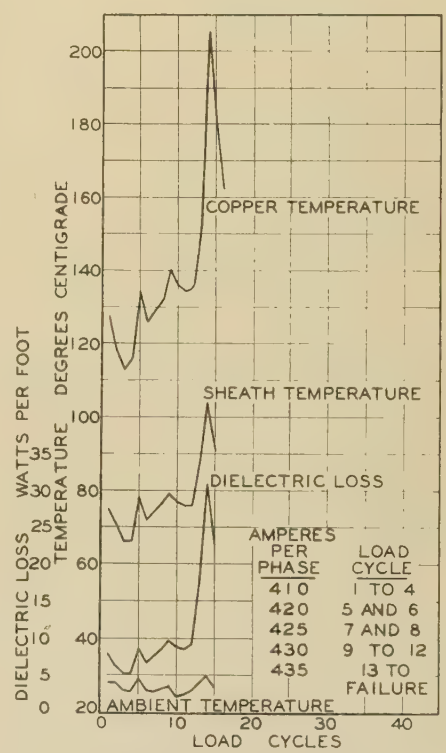

Figure 2. Load-test measurements at three phase, 13.6 kv, 60 cycles

Three-conductor 250,000-circular-mil 15-kv cable

During the testing period a total of 30 cases of failure and removal before failure had developed on the 250,000-circular-mil cable in these feeders. Between the period in which these cables had been removed from test and November 1938,

12 additional failures occurred. A summary of the classification of this trouble is shown in table I.

The cable in the feeders under test was made up principally of three different makes. The cable of one manufacture had an extremely high rate of dielectric failure compared to the other makes of cable. Comparison of cables by manufacturers is shown in table II. The various makes of cable were distributed among the four feeders tested as shown in table III and for comparison the range of duct temperatures in which these feeders operated is shown below the respective feeder numbers. While the one make of cable which showed a very high rate of dielectric failure was confined principally to one feeder, this feeder was installed in duct banks which had lower empty-duct temperatures than the other feeders. It does not seem probable that the high failure rate was due to operating conditions more severe than on the other three feeders tested.

Examination of the cable samples showed that in 14 cases of the 18 dielectric failures evidence of carbonization of the paper and occasionally of the compound appears in the conductor insulation adjoining the burnout area. This carbonization ranged from light and dark colored bands on the tapes to a charred mass of filler in the cable crotch.

In several instances the carbonization was strictly local, suggesting nonuniformity in the cable or excessive local duct temperatures, such as a steam-main crossing might occasion. In fact, six of the carbonization cases were definitely associated with copper temperatures of 110 degrees centigrade or higher as calculated from empty-duct measurements. In the remaining eight cases where carbonization of the paper, as well as of the compound, was found, these sections of cable were not believed to have been operated at temperatures in excess of 100 degrees centigrade during the loading test.

In order that the investigation be not confined solely to those sections of cable which had failed, six sections from these feeders were removed before failure for visual examination of dissected specimens and laboratory tests to determine dielectric loss and thermal resistance of the insulation. The sections selected had been subjected to from 225 to 366 cycles of test loading. There were specimens of each of the three different makes of cable. Physical inspections showed no marked difference in the appearance of the insulation made by any of the manufacturers. Varying degrees of saturation were found but most of the insulation was fairly dry. The compound was frequently found to have become discolored and hardened, particularly next to the conductor. In two instances flecks of carbonized compound were discovered but in one of these cases the carbonized specks occurred at a point in the cable which had been at a higher temperature caused by a steam main. No carbonized paper was found.

The results of laboratory tests on these removed samples of cable showed that their measured thermal resistivity was from 8 to 24 per cent above the value of 700 degrees centigrade per watt per centimeter cube which value had been used in computing the rated current for the load tests. While these test values were higher in each case than those measured on cable samples removed prior to the loading tests no change was made in the value previously used in computing copper temperatures. It is quite possible that copper temperatures may have been a few degrees higher or lower in some instances than those calculated. The dielectric-loss measurements on these samples were different in some cases than the values obtained on samples preceding the starting of load testing at which time a dielectric loss of one watt per foot had been assumed for a copper temperature of 100 degrees centigrade. Typical measured values of dielectric loss for these removed samples are shown in figure 3. These values are shown to indicate the relative variations between makes of cable. The actual dielectric loss when operated at 11.4 kv, 25 cycles, would be approximately one-half of these values. It will be noted that the samples of cable of manufacturer *C* are among the highest values for each temperature. This was the make of cable which had the highest dielectric-failure rate.

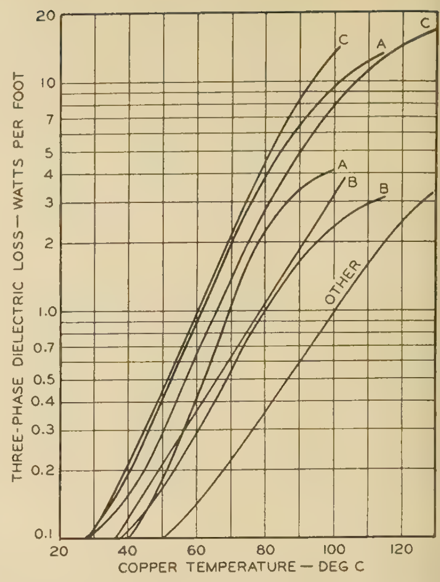

Figure 3. Dielectric loss measurements, 13.6 kv, 60 cycles

Three-conductor 250,000-circular-mil 15-kv cable

Several specimens of 250,000-circular-mil cable from a faulty feeder which had not been subjected to load tests were examined. Striations of carbonized paper and compound were noticed near the fault and evidences of carbonized paper near the conductor were found at several other locations. The presumption is that this portion of the feeder had been subjected to overloads at some previous time. The presence of carbonized paper in these samples serves as a warning that the test load cycles must not be charged too readily with the full responsibility for similar conditions in samples from the tested feeders.

Table III. Distribution of Failures in Feeders, Long-Time Test on Three-Conductor 250,000-Circular-Mil Cable

	Number of Sections Tested and Number of Faulty Sections							
	Feeder No. 1		Feeder No. 2		Feeder No. 3		Feeder No. 4	
Manufacturer	Tested	Failed	Tested	Failed	Tested	Failed	Tested	Failed
A	16	1	11	2	4	0	18	2
B	3	0	2	1	16	0	1	0
C	16	13	3	1	3	0	0	0
Other	0	0	1	0	20	0	0	0
Unknown	0	0	2	2	11	2	2	1
Average empty-duct temperature	31		35		35		30	
Maximum empty-duct temperature	37		37		38		42	

Long-Time Field Tests on Modern-Type Cable

After nine months of load cycle testing on old 250,000-circular-mil cable, it was decided that similar long-time tests should be started on a small amount of three-conductor 600,000-circular-mil shielded cable, 220 mils insulation, 125 mils lead, in order that it might be pos-

Table IV. Classification of Trouble During Test, Long-Time Tests on Three-Conductor 600,000-Circular-Mil Cable

Cause of Trouble	Number	Per Cent
Cable		
Dielectric failure	0	0
Lead-sheath failure	8	62
Miscellaneous	2	15
Joints		
Cracked sleeves	3	23
Total	13	100

Table V. Distribution of Failures as to Time of Occurrence, Three-Conductor 250,000-Circular-Mil Cable on Short-Time Tests

	Number of Failures	Per Cent
During test period		
Preliminary kenotron test	3	3
During loading test	21	24
Final kenotron test	7	8
	31	35
After load testing		
Operating failures	22	25
Test failures	29	34
Removals before failure	5	6
	56	65
Total	87	100

sible to determine whether or not the effects of these heavy loads on modern-type cable is different from the effect on the older type of cable.

Such a test was started in April, 1935, on two miles of this cable in one feeder which had been in service about five years. The cable had been purchased in 1929 and 1930. Joints on this cable were filled with petrolatum. The feeder containing this cable was tested until August 1936 and again from October 1937 to May 1938, a period of about two years. More of this type of cable, 4,000 feet long, was put on test in December 1937 and is still under test. The interruption of the test on the first feeder was made necessary by operating conditions.

During the period of these tests a total of 13 troubles have developed. One trouble developed on this cable during the period when the feeder was temporarily removed from test. The causes of the troubles which occurred on this cable during the tests are shown in table IV.

The trouble rate on this cable during the period that the test was in progress was high compared with similar cable not under test. During the interval the test was discontinued, there was a marked decrease in failures in this cable.

An examination of the sheath and insulation was made on all cable removed from service. The sheath in some cases was slightly swollen but not excessive as might be attributed to a "ratcheting" action as occasioned by successive load cycles. The cable insulation in all cases was in good condition and showed no signs of carbonization. The troubles caused by cracked and swollen joints cannot be attributed definitely to the load test as the joints in manholes were not inspected prior to the test and several similar cases have been found on cable operated at normal copper temperature. It seems quite probable that a great number of sheath failures can be directly attributed to the test loading.

Short-Time Field Tests

After about nine months' experience with cyclic loading to 100 degrees centigrade copper temperature on the four feeders containing 250,000-circular-mil cable, it was found that most of the failures had been due to dielectric heating, and relatively few were due to mechanical trouble. It was tentatively decided to rate cable of this type and vintage for emergency operation at current values corresponding to 100 degrees centigrade copper temperature. The feeders containing such cable were for the most part made up with cable having larger-size conductors than 250,000 circular mils, and it was felt that these higher operating temperatures could be tolerated, even should they cause increased operating failures, in order not to unduly limit the capacity of the feeders.

Beginning April 1935, a series of short-time field tests was begun. This was undertaken because it was felt undesirable to increase the rating of some 74 feeders containing three-conductor 250,000-circular-mil cable similar to that in the long-time test feeders without attempting to precipitate incipient failures which might otherwise occur at a more inconvenient time from the operating standpoint. Cyclic loads calculated to result in maximum copper temperatures of about 100 degrees centigrade were applied for eight hours each working day for two weeks. Sixty-six of a total of about 74 feeders were tested in this manner. The remaining feeders could not be tested because of operating difficulties in obtaining the desired loads. There is a total of 72 miles of three-conductor 250,000-circular-mil cable in the tested feeders. The ratings of about 50 feeders containing three-conductor, 250,000-circular-mil cable have been increased to a value of current corresponding to 100-degree-centigrade copper. It was proposed that the rating of the other feeders be increased later. Notwithstanding the increase in the rating, it was found in December 1937 that the loads actually carried had increased very little above those which these feeders carried prior to the testing. As a result, the operating record of these cables since the conclusion of the testing cannot be considered indicative of their operation at loads corresponding to 100 degrees centigrade. Eighty-seven cases of trouble have occurred on this type of cable in these 66 feeders during a period of $3^1/_2$ years. Sixty-nine of these were cable failures and 18 were joint failures. The distribution of failures as to time of occurrence is shown in table V. The ascribed causes of failure in cable are shown in table VI.

In two of the cases where the cause was ascribed to dielectric failure, the cable had carried loads far greater than the test load and in two other cases the faulty cable was found to have been near steam-main crossings which increased the duct temperatures to values which gave calculated copper temperatures over 100 degrees centigrade.

Carbonization of the insulation adjacent to the failure or in end samples was observed in 28 cases. The degree of this carbonization varied over the same wide range noted in the discussion of the long-time tests on this cable. The cable insulation in many of the remaining cases of failure was found to be generally desaturated and dry and carbonized compound was often found.

The results obtained on these tests

Table VI. Classification of Troubles, Short-Time Test on Three-Conductor 250,000-Circular-Mil Cable

	During Test*		After Test		Total	
Cause of Trouble	Number	Per Cent	Number	Per Cent	Number	Per Cent
Dielectric failure	20	80.0	24	54.5	44	63.7
Lead-sheath failure	2	8.0	12	27.3	14	20.3
Miscellaneous	3	12.0	4	9.1	7	10.2
Unknown			4	9.1	4	5.8
Total	25	100.0	44	100.0	69	100.0

* Includes failures due to kenotron tests which were made before and after the two-week loading periods.

Table VII. Comparison of Failure Rates

Failure Rates per 100 Mile-Years of Operation

Year	All 250,000-Circular-Mil Belted Cable			All Cable Operating at 11.4 Kv Except Three-Conductor 250,000-Circular-Mil Belted		
	Cable	Joint	Total	Cable	Joint	Total
1931	32.0	3.5	35.5	11.7	6.0	17.7
1932	22.9		22.9	9.0	4.5	13.5
1933	18.5	4.8	23.3	8.8	3.1	12.0
1934*				9.7	3.6	13.3
Jan.–May* 1934	15.9	4.4	41.8			
June–Dec.* 1934	38.8	3.0				
1935*	66.8	17.6	84.4	16.1	5.9	22.0
1936*	55.6	4.1	59.7	13.0	5.5	18.5
1937*	12.8**	9.5**	22.3**	12.4	2.7	15.1

* These rates include removals before failure. Long-time load tests were in progress on one or more feeders between July 1934 and February 1937. Short-time load tests were in progress on one or more feeders between April 1935 and July 1937.

** Rate obtained over period from May 1, 1937 to October 1, 1938.

indicate that the failure rate during the heavy-load testing was several times the failure rate usually experienced with that type of cable when operated normally. The failure rates of these same cables after conclusion of the testing was lower than the value during the testing and tended to approach that usually experienced with this type of cable as normally operated. The comparison of failure rates between old 250,000-circular-mil cable and all other cable operated at 11.4 kv is shown in table VII. A period of time was included to show the operating experience prior to and subsequent to the load testing. It will be noted that the vintage of cable tested had had a consistently higher failure rate than other cable before the test. During the test period, the failure rate increased markedly and at the conclusion of the tests it had closely approached the average of the other cables.

Conclusions

1. Operation over a continuing period of load cycles to copper temperatures of 100 degrees centigrade of old types of paper-insulated cables which were dry and contained rosin, results in a marked increase of dielectric failures.

2. Similar operation of modern well-impregnated paper-insulated cables resulted in no failures caused by dielectric heating or aging.

3. Both old types of cable and modern types of cable had an increase in the rate of failure due to wearing and cracking of the lead sheath when operated at these load cycles.

4. It is our opinion that the occasional operation of paper-insulated cables during emergency conditions to copper temperatures as high as 100 degrees centigrade can be justified where a small number of sections of cable in any feeder otherwise set up loading restrictions.

5. It is our opinion that those types of modern cable which provide means for taking care of the expansion and contraction of the cable compound, such as not to unduly stress the lead sheath, could be operated repeatedly to copper temperatures as high as 100 degrees centigrade.

6. It is our hope that others may be interested in carrying on similar operating experience on cable systems of this and other voltage classifications, in order to obtain a more widespread knowledge of practical operating limitations for paper-insulated cable.

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Discussion

L. I. Komives (nonmember; The Detroit Edison Company, Detroit, Mich.): It would be interesting to know the distribution of cable failures due both to the dielectric and to the lead sheath in regard to its location, that is, what percentage of failures were in the manhole, near the manhole in the duct, and in the duct section.

Conclusions drawn in the paper seem to be less enthusiastic than in the presentation by the author. However, in either case, the author and his company should be congratulated in taking such a bold step to find out the economical life of a highly loaded cable.

It seems to be a moot question whether the fact that an overloaded cable cannot be salvaged to be reused was taken into consideration when economical loading of cables was considered by the author.

G. B. Shanklin (General Electric Company, Schenectady, N. Y.): I desire to discuss briefly maximum safe operating temperature of underground cable as covered by the two outstanding papers presented by Franklin and Thomas, and Halperin.

Each of these two papers has made a valuable contribution to cable engineering. The paper by Franklin and Thomas is outstanding in giving us for the first time a systematic study of controlled overloading of cable under actual field conditions. I have always felt that this problem would never be solved in the laboratory and that practical knowledge could only be gained in the field, because of the large number of variable factors involved. It is not possible to reproduce these in the laboratory.

Mr. Halperin's paper is outstanding in giving us for the first time a systematic study of cable movement under load conditions in ducts. Now that Mr. Halperin has placed this problem on a scientific basis we need an extension of this information covering all possible conditions of field service. The severe duty requirements on lead sheath, especially with modern, well-impregnated cable, represents a limiting factor in determining maximum safe operating temperature for cable.

An analysis of the data records in both papers leads to the same important conclusion, which is, that when underground cables are loaded above what we now consider normal loads, there is a noticeable increase in the rate of service failures. This is true whether the cable is of the old poorly impregnated type or the modern well-impregnated type using thinner compound. Overloading of the older cable leads to dielectric loss failures and fatigue of the lead sheath due to cable movement. Overloading of modern cable causes an even greater burden on the lead sheath, and the majority of service failures are due to lead-sheath troubles. With modern cable, overloading causes greater stretching of lead sheath which not only adds to the sheath troubles mentioned but aggravates migration of compound, leading to ionization damage in the drained sections of cable. Thinner insulation and higher working stresses in modern cable do not allow pronounced drainage of this kind, and this can only be avoided by keeping the operating tempera-

tures within reason. The same lesson has been learned in Europe, and maximum allowable copper temperature is invariably lower than allowed by our present standard rules.

At the AIEE winter convention in 1921 a symposium of six papers was presented on exactly the same subject. In discussing these papers the present writer summarized the various factors involved and then made the following statement:

> When these factors are considered, it is apparent that the temperature limit of (low-voltage) paper cable insulation should be something less than 105 degrees centigrade. If the temperature limit of 85 degrees centigrade is to be raised, this change must be approached with caution and an increase to 90 degrees or 95 degrees centigrade is all that should be attempted until more is known of the subject.

Thanks to the authors of these two papers and the experience we have all gained since 1921, we now know more about this subject. Nevertheless, the above statement still holds. The margin for temperature increase to take care of emergency loads is without question quite narrow, and the data records in the two papers by Franklin and Thomas, and Halperin show that an increased rate of service failures must be accepted.

The writer does not believe that the cable industry is yet in a position to draw up safe emergency overload ratings for cable that approach the temperature limits suggested by these two papers, in the order of 100 degrees centigrade for 15-kv cable. This will definitely mean an increase in service failures. The burden on the lead sheath is greater than it used to be. If this is a limiting factor, as indicated by these two papers, we must know more about the general conditions of cable movement and training of cables in manholes. Many of these manholes around the country are relatively small and extremely crowded, with short radius of bend on the cable. In drawing up national standard rules we must protect both the users and the manufacturers who must meet these kinds of conditions. This in no way precludes the use of emergency overload ratings under favorable conditions and where performance can be watched closely. These special conditions cannot very well be included, however, in standard rules covering all possible field conditions.

Temperature increases for emergency load rating as proposed by Mr. Halperin are outlined in table X of his paper. In my opinion the proposed temperatures should be decreased by at least five degrees centigrade in some cases and by ten degrees centigrade in other cases for safe use around the country. This would mean that there would be no margin at all for 66-kv single-conductor cable. The present normal load limit of 60 degrees centigrade for cable of this voltage rating is already too high.

Wm. A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): The papers by Franklin and Thomas and Halperin present a mass of data tending to show that the present standard temperature limits for paper-insulated cables are not sufficiently high, as far as the dielectric is concerned but, if materially exceeded, may lead to early sheath and joint trouble. Such developments as may occur in solid-type cable in the near future are more likely to increase the stability of the dielectric than the life of the lead and, therefore, will not raise the temperature limit.

Moreover, some effects not anticipated may limit the tendency toward higher temperatures such as, for instance, the cumulative effect of soil drying around duct lines operated at very high temperatures or the eventual rise in power factor which must result from ionization during cold periods of the exaggerated load cycle. It must be remembered that the tests were at rated voltage, not under accelerated aging conditions as far as voltage is concerned, and that a two-year test only indicates what will happen in two years, not five or ten years. Accelerated aging tests at voltages well above those of normal operation do not show much change in power factor or hot-spot-temperature development in the early stages but, at some unexpected moment, start up rapidly, as a prelude to failure.

J. A. McHugh (Consolidated Edison Company of New York, Inc., New York): Outstanding features of this paper appear to be the importance of the condition and mechanical construction of paper insulated cables in determining their ratings.

1. The cables of the vintage of a quarter century ago show, as the authors point out, a wide difference of present condition, overloads sustained in more rigorous years being a serious factor. Perhaps distention of the sheath and absorption of excess compound by the paper are causes of their high dielectric loss. The use of the load cycle test at high temperature appears, from the data of table V, to be effective in precipitating voltage-test failures and removing the poorer cable from service at convenient intervals.

2. The modern cables, of essentially "hard" core construction, are limited in their ratings by the sheath, as the authors have indicated in table IV.

3. Conclusion 5 regarding modern cable which provides means for taking care of the expansion and contraction of the cable compound is inevitable. Distinction might well be made between constructions which simply control the internal hydrostatic pressure and those which also maintain continuous impregnation, in further investigations.

It is to be hoped that this investigation, so logically developed, will be helpful in increasing the economic availability of the improved cable constructions.

E. W. Davis (Simplex Wire and Cable Company, Cambridge, Mass.): Conclusion number 4 states that 100 degrees centigrade is justifiable for occasional operation. It is interesting to notice how closely this represents the general opinion of 18 years ago in 1921, as indicated by the AIEE symposium on this subject at the winter convention of that year, particularly if allowance is made for the reasonable differences of opinion that existed then.

The authors' test results indicate that the principal limitation to more severe service is sheath trouble rather than actual temperature deterioration of the paper. This is further reason for the present-day tendency to improve the quality of lead sheath and to develop superior types of sheath where possible.

In connection with the interesting superiority of the new cable over the old we are wondering to what extent this is due to the fact that the new cable was type *H* and the old was belted. Other things being equal, type *H* cable is somewhat superior to belted in allowable operating temperature.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): The information, given in this paper, on trials in the laboratory and in service of unusually high operating temperatures for high-voltage cables, constitutes an important and comprehensive contribution on the subject.

The laboratory tests show that temperatures over 130 degrees centigrade are too high for these old rosin-impregnated transmission cables. However, this value is considerably above the present limit of about 75 degrees centigrade for the cable involved, and much can be gained by using some limit between these two values.

In the field tests the authors assumed that the dielectric losses amounted to one watt per foot although their figure 3 shows much higher measured values. Also, the authors found that the thermal resistivities of the insulation were from 8 to 24 per cent higher than the values that they had assumed.

The authors state that in eight cases carbonized paper was found in cable that was believed to have operated at temperatures less than 100 degrees centigrade. Could not these temperatures have been much higher, also, due to higher values of dielectric loss and thermal resistivity than those assumed?

The paper indicates that high rates of trouble occurred during the overload tests but that some weeding out of weak spots occurred with a decrease in troubles for a short time after the tests. The authors seem to suggest that the weak spots be eliminated by an overload test before the emergency ratings are increased. The emergencies during which the temperatures might approach the limits occur at very infrequent intervals. Probably by the time the occasion arrived, other weak spots would have developed. To be effective, then, the elimination testing would be necessary at regular periods.

Trials of higher temperatures of the kind discussed constitute an important step toward more economic use of underground cables. For full effective use of the results, more detailed analysis of the troubles experienced seem necessary, especially in regard to the increases in troubles due to ionization, dielectric losses, sheath cracks, etc., incidental to operation at the increased temperatures.

W. F. Davidson (Consolidated Edison Company of New York, Inc., New York): In support of the data presented in substantiation of the suggested higher operating temperature for paper-insulated cable, I should like to present some data secured about five years ago in connection with a rather extensive investigation of 27-kv paper-insulated cables. The particular study was initiated for the purpose of determining the practicability of reducing insulation thicknesses but as it progressed it became apparent that the most valuable information was concerned with operating temperature limits. The cables were three-conductor 350,000-circular-mil shielded type with 280 mils insulation. Samples were

made by two manufacturers each of whom supplied approximately 4,000 feet. Cables for test were installed in the street and in addition 100-foot lengths were tested by load cycle procedure in the laboratory. The number of daily load cycles ranged from 109 to 114. Maximum load temperatures varied from 70–75 degrees centigrade to 100–105 degrees centigrade with a few load cycles falling within other temperature ranges. The principal criteria of cable performance were the increase in dielectric loss and the reduction in life of residual samples when tested at room temperature and four times rated voltage. An examination of the detailed test results shows that the room temperature dielectric loss was practically unchanged as a result of this load cycle testing. There was a progressive increase in the dielectric loss at elevated temperatures but an analysis of the data has failed to show any clear cut difference in the rate of increase during the 60 load cycles in temperature range 70–75 degrees centigrade as compared with the 35 or 40 load cycles when temperature ranged 100–105 degrees centigrade. Such differences as may exist are obscured by the inevitable variations in observed values.

After completing the load cycle tests, five-foot specimens were removed and tested to destruction at four times rated voltage to obtain a measure of the comparative residual life. For one make of cable, the duration to failure was 4.5 hours as compared with more than 570 hours for samples that had not been subjected to load-cycle testing. For this make of cable, the dielectric loss at 80 degrees centigrade had increased to a value 90 per cent above that obtained on tests made at the factory. For the other make, the life of samples after load cycle aging was 11 hours as compared with 40 hours for cable which had not been subjected to the test and the dielectric loss at 80 degrees centigrade had increased sixfold.

C. W. Franklin and E. R. Thomas: In reference to the question of L. I. Komives regarding the distribution of cable failures as to location of failure, 85 per cent of the dielectric failures occurred in the duct, 10 per cent in the manhole, and the remaining at the duct edge or unknown. Similarly 51 per cent of the sheath failures occurred in the manhole, 40 per cent at the duct edge, and the remaining in the duct.

We are in agreement with E. W. Davis that sheath troubles constitute one of the major limitations in the operation of solid-type paper-insulated cables and that everything possible should be done to improve the quality of cable sheaths. While the dielectric failures found during the heavy load testing were confined entirely to old belted cable, and while age and type of cable both probably were contributing factors, we feel that the age of the cable more than the type of cable was the major contributor to the dielectric failures. The impregnating compound used in cables of the vintages tested had dielectric loss characteristics which rose very sharply with temperature and thus tended to make for greater instability of cable operation at the higher temperature levels.

In reference to Herman Halperin's comments regarding the dielectric loss values used, the test values shown in figure 3 were measured at 13.6 kv, 60 cycles, while the cable under test in the field was operating at 11 kv, 25 cycles. The average dielectric losses assumed to be about one watt per foot at the operating conditions are believed to be in agreement with data obtained on test lengths. The eight cases where carbonized paper was found in cable which was believed to have operated at temperatures less than 100 degrees centigrade are based on assumed values of dielectric loss and thermal resistivities. It is recognized that deviations in these latter quantities may easily have been of the order of ten per cent. Considering these deviations and measured duct temperatures we believe that during the test period these particular cables had not exceeded the 100 degrees centigrade copper temperature. It is quite probable that these cables some time during previous periods of operation may have been subjected to loads which might have caused carbonization of paper to develop. The use of the high-load tests for a short time on the various feeders was believed to be a satisfactory way of eliminating those sections of cable having unstable dielectric-loss characteristics when operated at copper temperatures of 100 degrees centigrade. The test, however, was not intended to determine the thermal limitations along the particular feeder as these had previously been checked by temperature survey. We did feel that this method of test would show up existing unstable cable and that it would not necessarily have to be repeated at any particular period unless it was felt that there was progressive continued deterioration going on in the cable on the feeder.

The data on aging tests conducted on 27-kv cable which W. F. Davidson reported is interesting in showing that the elevated temperature operation did not tend to show a decrease in dielectric stability.

In answer to W. A. Del Mar, we have not experienced the effect of soil drying around our duct lines in such a manner as to create an unstable thermal condition for the duct bank. The fact that these load-cycle aging tests were conducted at normal voltage seems to us to be more convincing evidence of the cable operation ability than short-time tests at overvoltages.

We are in agreement with G. B. Shanklin that paper-insulated cable can well be operated at temperatures of from 90 to 100 degrees centigrade without probable injury to the dielectric but that cable-sheath design and technique of manufacture constitutes the greatest drawback to operating our existing cables at these temperatures.

In closing, we feel that as the result of the papers presented on cable operation at the higher temperatures and the resulting discussion of these papers, we come to the generalization that oil-impregnated paper insulation as used in modern cables is not in itself affected when operated at temperatures up to 100 degrees centigrade but when insulation is incorporated as an integral part of cable it may be subjected to some increase in deterioration due to resulting void spaces which may form due to heating and cooling cycles. The sheath problem, on the other hand, is one of a material limited in mechanical strength and affected more from stresses set up in it due to changes in loading than to the magnitude of the temperature. A satisfactory solution of our cable-sheath problem is probably of more immediate concern in securing a more economical high-voltage cable than the development of improvements in oil and paper dielectric.

Transactions

Papers and Discussions Comprising Pages 563–610 of the 1939 Volume

Voltage Control of Mercury-Arc Rectifiers

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THE mercury-arc rectifier competes with the older well-established types of apparatus, the synchronous converter and motor generator set. Both of these types of apparatus are flexible in application as they may be adjusted through a range of different output characteristics and in the case of the booster-type converter and motor generator set, considerable range in voltage control is possible. In order to take full advantage of the desirable features of the mercury-arc rectifier, its output voltage should duplicate that of its rivals.

The output of a rectifier and its associated transformer without any special means of modifying or regulating the voltage is similar to that of a shunt-connected generator in that an increase in load causes a reduction in the direct voltage. The characteristic is a straight line from light load to the rated capacity of the equipment. The slope of this characteristic is dependent upon the type of transformer connections used, the reactance and resistance of the transformer and supply system, and the arc-drop characteristic of the rectifier.[1]

The importance of the flat-compound characteristic has long been recognized particularly in the railway field. One of the earliest attempts at modifying the voltage output of a rectifier consisted of a scheme to produce the flat compound characteristic.[2] In figure 1 a commonly used rectifier circuit is shown, with the exception that the interphase transformer is provided with a saturating winding. If this transformer and its winding were short-circuited by the dotted connections x and y, a six-phase diametric connection would be obtained having an output characteristic as shown by AB.

If the dotted connection x is now removed the interphase transformer is placed in operation and six-phase operation is still obtained but the resulting connection is known as double three phase, the two wyes being forced to operate in multiple by the interphase transformer. The output of the rectifier is still six phase as the two three-phase systems are displaced from each other by 60 degrees. This characteristic is shown by ACD.

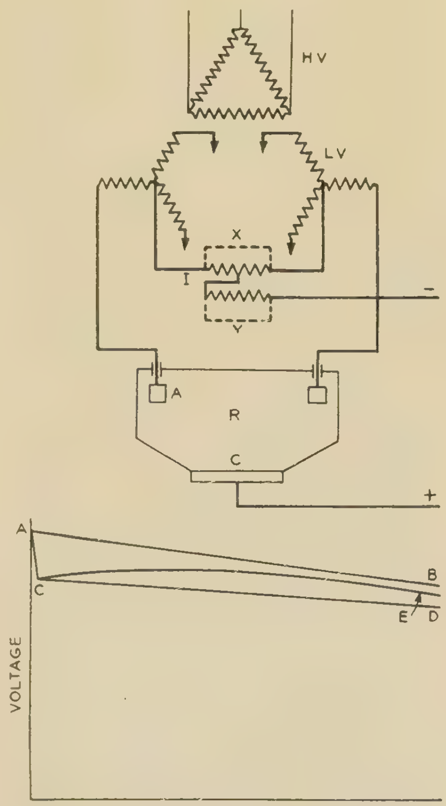

Figure 1

HV—Transformer high-voltage winding
LV—Transformer low-voltage winding
I—Interphase transformer
R—Rectifier
A—Main anode
C—Cathode

It is apparent that any gradual means of making the interphase transformers ineffective with an increase in load would produce a characteristic similar to ACE. If the short circuit across the interphase saturating winding is now removed and the interphase transformer is made ineffective by saturation in proportion to the d-c output, the desired compound characteristic would be obtained.

An equipment of this kind was described in the TRANSACTIONS of the AIEE. The actual characteristic obtained was shown in the article.[3]

The circuit for compounding described above has the disadvantage of employing saturation of the magnetic circuits, making it difficult to obtain a smooth curve of the desired shape. In addition it causes a poor power factor. To overcome these difficulties a more flexible scheme of compounding was developed as shown in figure 2. In this figure the interphase transformer has connected across it a circuit made up of a saturating reactor and capacitor. The capacitors used in this way advance the firing period of the rectifier anodes, thus compensating for the reactive part of the normal rectifier regulation. The point at which the capacitors have the maximum effectiveness is determined by the constants of the circuit. The advantages of this circuit are: increased flexibility in adjustment, better utilization of the main transformer windings, improvement in power factor, and decreased losses.

A simple way of changing a rectifier direct output voltage is to vary the alternating voltage applied to the rectifier. This can be accomplished by any of the commonly used methods for regulating or changing the voltage of an a-c circuit. In general, however, this type of voltage control is not suitable with rapidly fluctuating loads.

With progress in the art of rectifier design, control grids were introduced which were capable of modifying the starting point of the arc from the rectifier main anodes. By delaying the firing of the anodes, a reduction in the output voltage is possible. Voltage regulation by this means has been fully described in a number of articles.[5–7] This type of regulation is very flexible and any desired output voltage can be produced providing the desired voltage lies below the

Paper number 39-11, recommended by the AIEE committee on transportation, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 17, 1938; made available for preprinting December 5, 1938.

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1. For all numbered references, see list at end of paper.

natural output characteristic of the rectifier.

This type of voltage regulation imposes some additional duty on the main anodes. It makes no change in the main transformer but does cause an increase in the size of the interphase transformer if used. One of the main objections to excessive use of voltage reduction by grid control is the distortion which it produces in the a-c power supply system and the d-c output of the rectifier.

In order to produce this type of regulation it is necessary to obtain the proper sequence in making the grids positive with respect to the voltage of the rectifier cathode. One of the simplest means of doing this is by a Selsyn phase-shifting transformer as shown by figure 3.

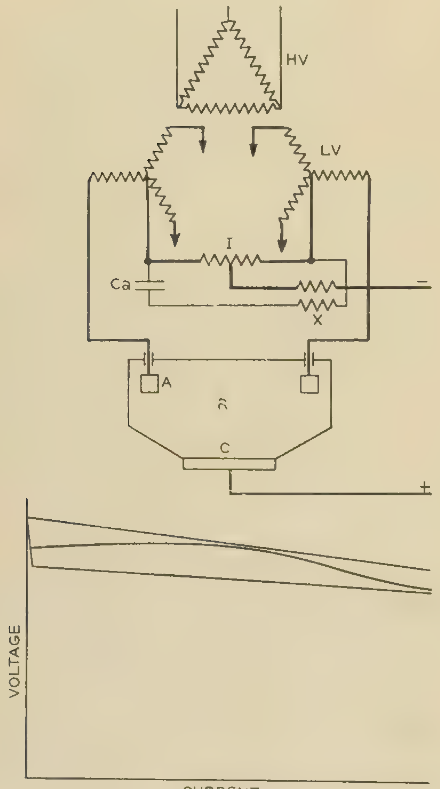

Figure 2

HV—Transformer high-voltage winding
LV—Transformer low-voltage winding
I—Interphase transformer
Ca—Capacitor
X—D-c saturating reactor
R—Rectifier
A—Anode
C—Cathode

The normal unregulated characteristic of the rectifier is shown by the curve *ABC*. When the Selsyn transformer is adjusted to retard the firing of the anodes and left in position, a characteristic curve parallel to the normal characteristic of the rectifier is obtained but at a lower voltage such as *ADE*. A number of equipments have been installed with control of this type so that the voltage of the rectifiers could be manually adjusted.

A simple modification of the scheme shown in figure 3 is the addition of a voltage regulator equipment and motor drive for the Selsyn transformer. With this equipment the regulator serves to maintain constant direct voltage. This type of regulation has been used for a number of equipments operating under steady load conditions where rapid control of the direct voltage is not necessary.

By means of a winding on the voltage regulator, carrying direct current in proportion to the rectifier output, the voltage can be made to rise or fall with an increase in d-c load as desired.

Load balance coils on the regulators will also balance the load quite accurately between two or more equipments. Figure 4 illustrates one method of obtaining a directional indication of the magnitude of the load being carried by a rectifier. The load indication is taken from a current transformer in the supply to the rectifier. It will be noted that if one equipment tends to carry more load than the other a current will flow through the equalizing coil circuits in such a direction as to cause the regulators to tend to boost the voltage of the rectifier carrying the least load and to reduce the voltage of the rectifier carrying the greater load. As both units are connected to the same bus, their output voltages cannot be different. The regulators therefore shift load between the units until the desired balance in output is obtained.

Although current transformers are shown in figure 4, other means of obtaining the load balance indication may be used such as using the drop across a shunt in the rectifier load circuit, the d-c drop through the interphase transformer, or any other convenient means depending on the particular installation.

For railway and similar types of service a more rapid type of regulation is desirable which would eliminate the slow-moving motor-driven Selsyn transformer. Accordingly, the circuit shown in figure 5 was developed in which the Selsyn transformer is still retained but merely for making the initial phase-angle adjustments. The necessary phase shifting of the grid voltage with respect to the voltage of the main anodes is obtained by applying a d-c bias to the neutral of the Selsyn transformer. The biasing voltage is obtained from a small d-c generator,

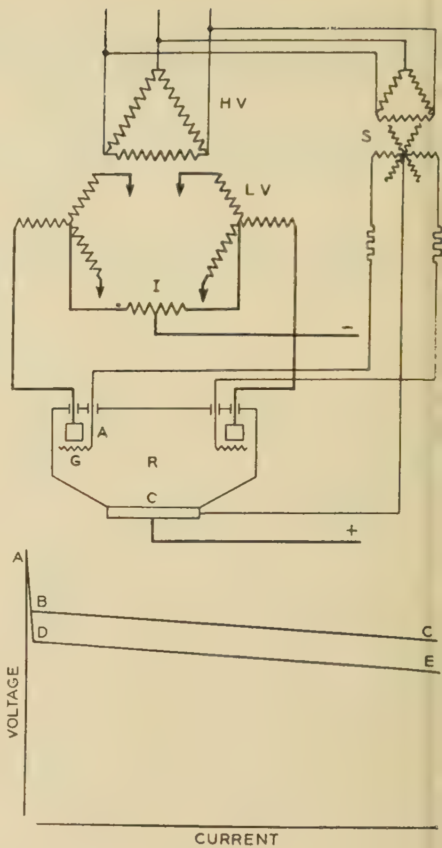

Figure 3

HV—Transformer high-voltage winding
LV—Transformer low-voltage winding
I—Interphase transformer
S—Selsyn phase-shifting transformer
R—Rectifier
A—Anode
G—Grid
C—Cathode

the output of which is controlled by the regulator. This does not produce any phase-angle shift of the a-c supply to the grids but by biasing the grid voltage there is, however, available a change in the time at which the grids go positive with respect to the cathode of approximately 90 degrees, although a much smaller shift is commonly used.

In figure 5 a biasing generator is connected between the cathode of the rectifier and the neutral of the six-phase grid transformer. This generator is provided with two separately excited shunt fields connected to oppose each other, one double the strength of the other. With this scheme of excitation the full range in voltage of the bias generator may be obtained in either direction. The output voltage of this generator is controlled by the regulator.

From the wave shapes shown in figure 5, keeping in mind that a rectifier anode cannot fire until its associated grid becomes positive with respect to the cathode, it will be noted that by means

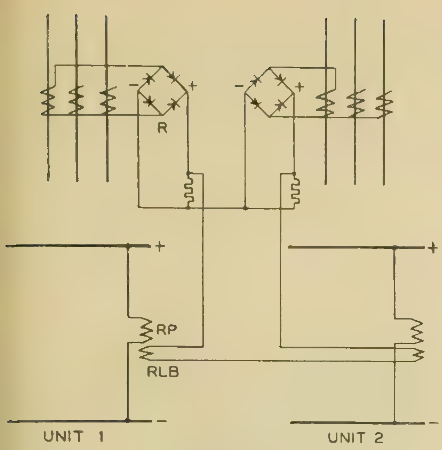

Figure 4

R—Copper-oxide rectifier
RP—Voltage regulator potential coil
RLB—Voltage regulator load balance coil

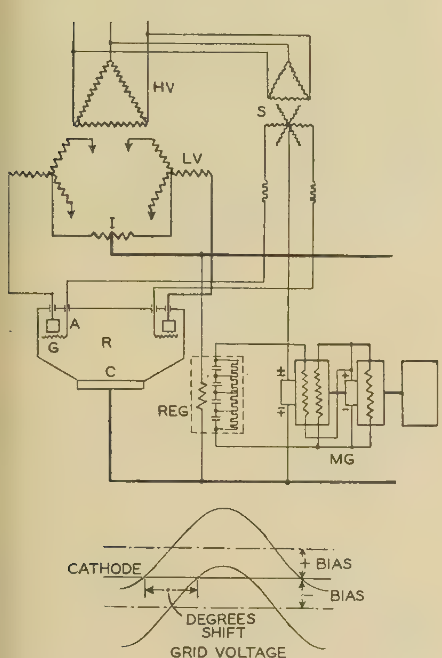

Figure 5

HV—Transformer high-voltage winding
LV—Transformer low-voltage winding
S—Selsyn transformer
R—Rectifier
A—Anode
G—Grid
C—Cathode
REG—Voltage regulator
MG—Grid-bias motor generator set

of this d-c bias there is a range of 90 degrees through which the firing of the main anode may be controlled. This gives a range from the normal unregulated voltage of the rectifier to approximately zero voltage if the load is sufficiently inductive to maintain the flow of current. This extreme range is not normally used but illustrates the flexibility of this type of control.

With this method a regulator can hold constant direct voltage or can be under- or overcompounded. The regulators can be equipped with load balance coils for balancing load between units.

The response of the rectifier to a change in grid voltage is practically instantaneous. This type of regulation is therefore very fast, the only delay being in the response of the regulator itself and in the ability of the separately-excited bias generator to change its voltage. There is no difficulty therefore in maintaining any desired voltage regardless of the fluctuations in d-c load demand.

As an illustration, this type of voltage control in railway service is being used with the equipment supplied for the electrification of the San Francisco-Oakland Bay Bridge.[8]

By substituting a current coil in place of the voltage coil of the regulator, the same scheme may be used to obtain a load-limiting effect; that is, as the current approaches limiting values, the voltage of the rectifier will be reduced to prevent any further increase in load. This scheme has been used in a number of installations. While they did not employ voltage control this feature together with load limiting could be applied by simply using a voltage regulator and a load regulator working on the same biasing generator.

This same type of control using a counter-electromotive-force generator has been used for voltage regulation of d-c generators. It is, therefore, comparatively simple to operate a d-c generator in multiple with a mercury arc rectifier with any desired load division between the units.

As noted above, grid control of rectifier voltage causes distortion in the a-c power supply and in the d-c output. The amount of this distortion increases with the amount of regulation. When long continued operation at reduced voltage is required, transformer tap changing under load and grid control forms a good combination. The a-c supply voltage is adjusted so that a minimum amount of voltage control with grids is required.

With the advent of the ignitor[9] a competing type of mercury-pool rectifier of the single anode type has become available and is being used quite extensively for various applications. An equipment of this type is made up of a number of separate tubes combined to give the equivalent of a multiple-anode rectifier. Each tube is fired in rotation by an ignitor which serves to start the arc. At the end of each conducting period the tube arc is extinguished and no ionization exists until the main anode again becomes positive with respect to the cathode and the ignitor is re-energized to provide ionization for starting the main anode.

Figure 6 illustrates the essentials of one method for controlling the operation of these tubes. The ignitor, a crystalline compound inserted into the cathode-pool mercury is energized momentarily each cycle to start the main anode. The operation during each cycle is as follows: When a main anode becomes positive with respect to its cathode the auxiliary vacuum tube is made conducting and current is passed through the ignitor. As soon as sufficient ionization is provided an arc is started between the cathode and grid of the power tube short-circuiting the ignitor circuit. This arc in turn starts the main anode. Voltage control is accomplished in the same way as with grid control by delaying the operation of the auxiliary vacuum tube.

The essential feature of this type of control is the provision of a momentary current supply to the ignitor for starting the main arc. In place of the scheme shown by figure 6, peaking transformers and other similar methods may be used,

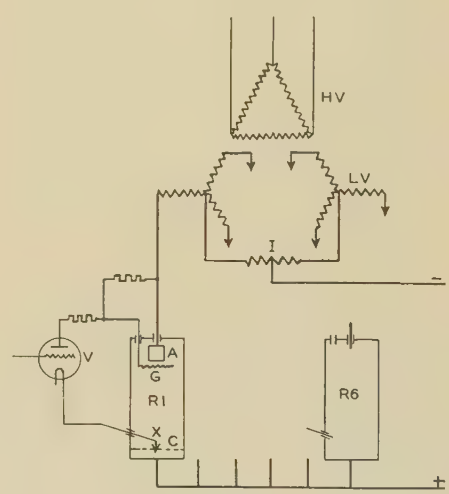

Figure 6

HV—Transformer high-voltage winding
LV—Transformer low-voltage winding
I—Interphase transformer
R1 to *R6*—Individual vacuum tubes
A—Anode
G—Grid
C—Cathode
X—Ignitor
V—Control tube

depending on the requirements of the application.

The foregoing outlines in general terms some of the progress made and some of the practical methods which may be used to control the output voltage of

mercury-arc rectifiers. In general, equipment is now available to regulate the output of a rectifier which will provide the functions of: flat voltage control, under- or overcompound voltage control, voltage control and current limiting, current control, and control suitable for other special applications, such as the equivalent of Ward-Leonard control for starting purposes. The rectifier equipment is thus in most respects as flexible in application as other types of conversion apparatus. The type of equipment to be used, and its control, will depend upon the requirements of the particular installation.

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Discussion

G. F. Jones (nonmember; Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. McDonald has discussed rather briefly voltage control of the ignitron rectifier, a typical circuit being shown in his figure 6. From the point of view of voltage control, this type of rectifier has distinct advantages which I believe should be emphasized.

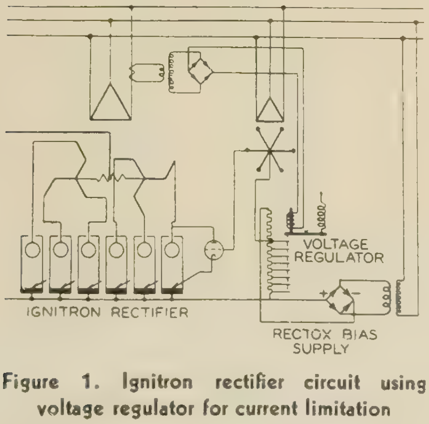


Figure 1. Ignitron rectifier circuit using voltage regulator for current limitation

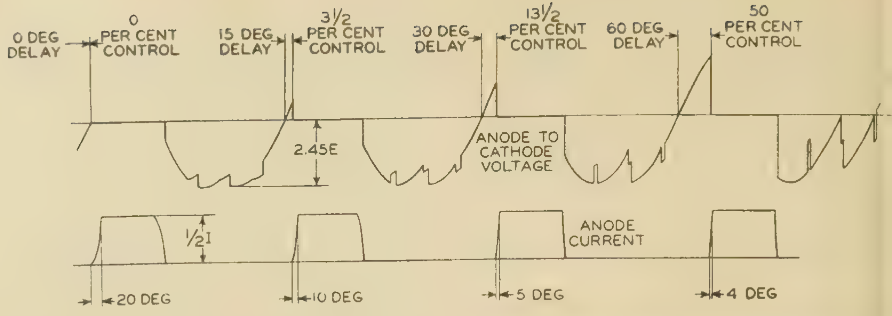


Figure 2. Showing anode-to-cathode voltage and anode currents in function of voltage control and angle of delay

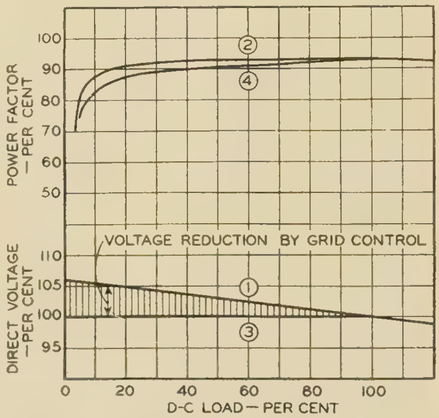


Figure 3. Voltage regulation and power factor of rectifiers with and without grid control

Curve 1—Voltage characteristic (drooping) without grid control

Curve 2—Power factor corresponding to curve 1

Curve 3—Voltage characteristic (level) obtained by grid control

Curve 4—Power factor corresponding to curve 3

As mentioned in the contemporary paper by Mr. Cox and myself, the arc voltage drop of the ignitron is the same for a voltage-controlled and non-voltage-controlled unit. For the conventional rectifier, the presence of the control grids in the arc path increases the arc voltage drop and therefore lowers the efficiency.

The power required by the grids of the excitation tubes of the ignitron is only a fraction of that required by the control grids of the conventional rectifier. This low power requirement extends the flexibility of the apparatus considerably and simplifies the control apparatus. For example, Mr. McDonald's figure 5 shows a motor generator set as a source of bias voltage to effect voltage control of a conventional rectifier. For the ignitron, the corresponding source can be a small Rectox, thus eliminating rotating apparatus and its inherent response time lag. Figure 1 of this discussion shows the essential circuit for high-speed voltage control of an ignitron. The voltage regulator operates to limit the a-c input to the unit to a constant value when the power demand is excessive. A potentiometer is connected across the fixed bias voltage of the Rectox source. The

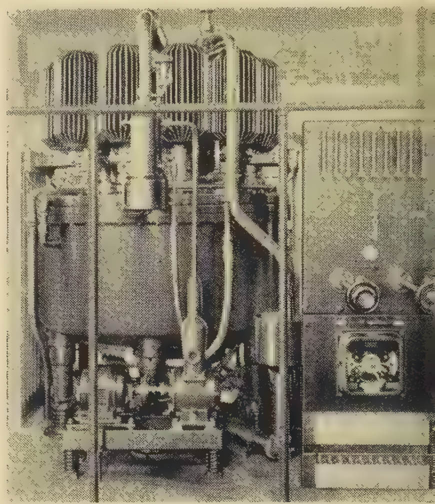


Figure 4. Grid-controlled rectifier installe[d] at a substation of the Commonwealth Ediso[n] Company in Chicago

resistance of the effective part of the poten[-]tiometer is varied by a Silverstat regulato[r] to insert bias voltage in the neutral of th[e] excitation-tube grid transformer in th[e] correct proportion to limit the current rise[.] In this circuit the only time lag is that o[f] the moving arm of the regulator.

I would like to ask Mr. McDonald i[f] there is not an error in his figure 6. Thi[s] circuit shows the ignitor current and th[e] ignitron grid current passing through th[e] same resistor. In order to conduct th[e] ignitor current, this resistor must have [a] low value. Connected as shown this woul[d] permit excessive grid currents both durin[g] normal conduction and during arc back.

O. K. Marti (Allis-Chalmers Manufacturin[g] Company, Milwaukee, Wis.): It was ver[y] gratifying to see from this paper tha[t] another manufacturer of rectifiers ha[s] adopted the d-c bias grid voltage contr[ol] and found that it offers many advantages[.] This method of control was used in con[-]nection with our grid-equipped rectifier[s] as far back as 1930 and has since then bee[n] incorporated in rectifiers for many differen[t] kinds of applications, as will be seen later o[n].

I would like to say a few words first i[n] regard to some statements which I fe[el] should be further amplified. For instance[,] it is said that the grid voltage contr[ol] imposes some additional duty on the mai[n] anodes. It is true that the rectificatio[n] phenomenon is made somewhat mor[e] difficult when the anodes are delayed i[n] their firing due to the fact that the revers[e] voltage increases more rapidly immediatel[y]

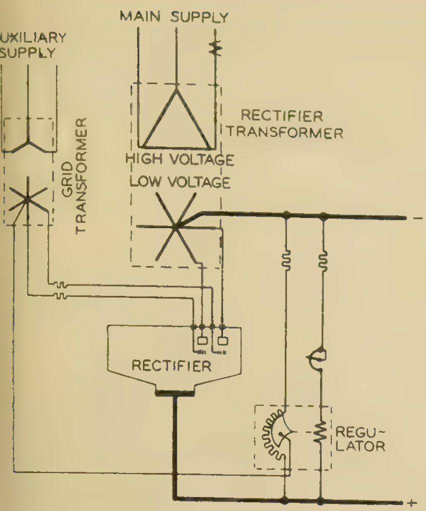
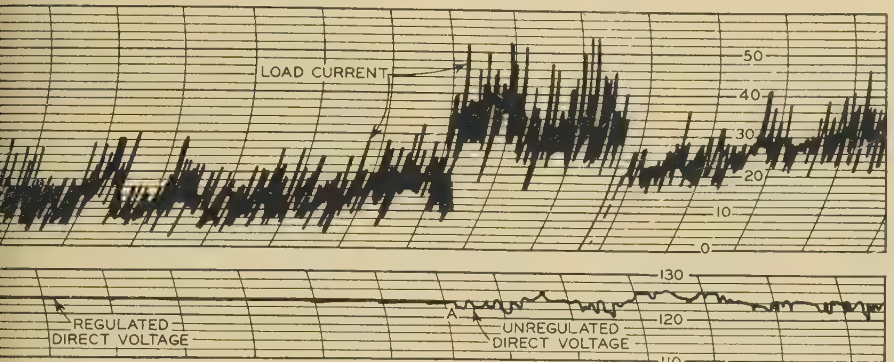


Figure 5. Diagram of connection



igure 6. Load charts showing actual voltage egulation obtained with grid-controlled ectifier furnishing power for railway load

Current multiplier—80 Voltage multiplier—5

ifter commutation takes place, however, is can be seen from figure 2 of this discussion, the anode-to-cathode voltage remains he same in magnitude. Furthermore, the inode currents do not increase appreciably, ind therefore a delay in firing does not iffect the loading of the anode, see figure 2, vhich shows the anode current for different ingles of delay and percentage of voltage control, respectively.

In most installations where only compounding of voltage is necessary, the reduction of voltage, and therefore the angle of delay is practically zero at full load, and becomes larger toward no load, as is shown in figure 3. Curve 1 shows the natural voltage characteristic of an uncontrolled rectifier, while curve 3 shows the flat voltage characteristic which can be obtained by means of grid control. In other words, the reverse voltage increases gradually as the rectifier load is decreased, and consequently there is no additional duty imposed on the anodes in such an application. In order to avoid delay in firing the anodes at full load the transformer taps are chosen accordingly. We therefore wonder to what kind of excess loading of anodes the author had reference.

The only other characteristics affected by grid voltage control and not mentioned by the author are power factors (see figure 3) and the wave-shape distortion of voltage and current of the primary as well as of the d-c system. These factors become quite important as soon as the amount of regulation of voltage is greater than the usual amount as required in railway service. In most applications, however, operation at greatly reduced voltages is not required, except for starting or for temporary periods of operation, as for instance when transferring load from one unit to another, or during very light loads, etc.

In figure 4 of this discussion is shown a 625-volt 5,000-ampere nominally rated 12-anode rectifier with automatic grid control equipment. This equipment was installed in a substation of the Commonwealth Edison Company in Chicago, and has now been in operation for over seven years. The control board is shown on the right-hand side and contains the necessary apparatus, including a quick-acting rocking-type regulator for automatically controlling the output voltage, as well as some rheostats and changeover switches for manual control of voltage.

From figure 5, which shows the simplified diagram of connections of the above installation, it can be seen that no auxiliary motor generator set is used, as in the application shown in figure 5 of Mr. McDonald's paper. However, instead of the generator voltage the rectifier output voltage is used as a source of the d-c bias. Such an arrangement simplifies the control considerably as only a standard potentiometer is necessary.

Figure 6 shows portions of voltage and current graphs taken in the above grid-controlled rectifier installation, which, as mentioned previously, supplies a railway load. These graphs clearly indicate the effectiveness of the grids in keeping the voltage constant in spite of continued fluctuations in the load current (shown in the upper graph). The automatic grid control was turned off at *A*, so that the portion of the (lower) voltage graph to the right of point *A* represents, by contrast, the unregulated fluctuating voltage of the rectifier. It is therefore evident that the output voltage of a rectifier may easily be maintained within as narrow limits as required in spite of fluctuating loads.

Another very interesting installation using this type of grid control was put in operation over two years ago by our Company in New York, furnishing power to the Third Avenue Railway. However, with this rectifier the grid control acts as a current regulator in that its function is to keep the current constant down to the full-load value as long as the base load of the station is above the rated rectifier load. The rectifier is operating in parallel with rotary converters in the same station. It is, however, connected to a different supply bus than the rotary converters. In other words, this installation is also interesting due to the fact that a 1,000-kw rectifier ties together two very powerful networks of 25- and 60-cycle frequencies, which could only be accomplished safely by using, instead of a rotary converter, a rectifier which is not susceptible to frequency or voltage variations.

Furthermore, a d-c bias control with a rocking-type regulator was used in an installation for the Republic Steel Company in Chicago. The arrangement for balancing the load is the same in principle as shown in figure 5, except that the drop across the shunt in the rectifier load circuit was to be used in order to affect the compensating coil of the rocking-type regulator. In

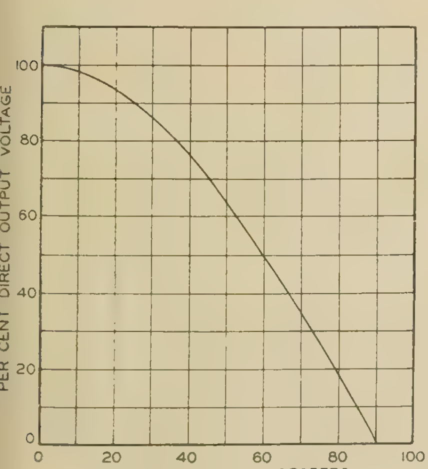
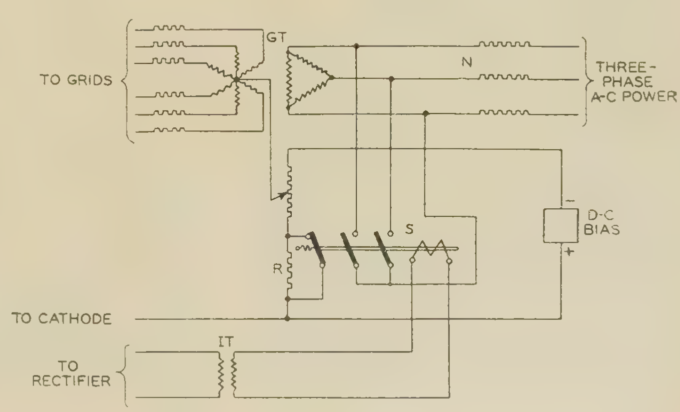


Figure 7 (left). Regulation of direct-voltage output of a six-phase rectifier as a function of the angle of delay

Figure 8. Grid-controlled protection circuit for interrupting short circuits and backfires



connection with this installation it was extremely important to obtain a voltage control over a wide range with quick response due to the requirements of the wire-drawing machines and of the excitation of the machinery in the automatic galvanizing plant. It was found that the d-c bias control on the rectifier met the requirements very well and takes care entirely automatically of a-c and d-c feeder voltage drop and of the inherent machine regulation.

In two radio rectifier plants we probably made the most extensive use of electronic grid control, in that the voltage is regulated from practically zero to the full value. It is general practice in radio installations to apply voltage first at a low value and then to increase it automatically in two or three steps to the normal operating value; see figure 7 of this discussion.

In a rectifier without grids, power is interrupted for protection purposes by means of an a-c circuit breaker in the primary power supply line. In a grid-controlled rectifier, however, additional protection can be provided by means of electronic interruption of power obtained by the blocking action of the grids. This blocking action is dependent upon automatically placing a negative potential in relation to the cathode on all grids when an overload or short circuit occurs in the radio transmitter or rectifier equipment. By using a small high-speed relay to apply the negative grid blocking bias, it is possible to interrupt power in the rectifier unit within a fraction of a cycle. This is considerably faster than the interrupting time required by an a-c circuit breaker, and thus grid control provides much better protection than mechanically operated circuit breakers. Moreover, since an a-c circuit breaker is usually furnished in any event, double protection is provided in that the high-speed grid-control protection apparatus is backed up by the slower-speed circuit-breaker equipment.

Figure 8 shows a diagram of a grid-control protection circuit. Upon the occurrence of a fault in the transmitting equipment, a surge current is induced through insulating current transformer IT on the coil of relay S. Opening of the back contact of this high-speed relay instantly inserts resistance R into the bias potentiometer circuit and thereby causes the negative biasing voltage to be increased to a value of greater relative magnitude than the positive a-c potentials placed on the grids through resistors N and grid transformer GT. Closing of the other contacts of relay S an instant later short-circuits the grid excitation transformer, thus doubly assuring that ony a negative blocking bias is maintained on the grids.

It could be seen from the above that by adding very little extra equipment, an additional possibility of the grid control can be realized. Interruption of short circuits and backfires is accomplished very successfully in connection with our mercury-arc rectifiers furnishing power for the New York subway. Measurements have shown that the backfires are interrupted in less than two cycles.

In connection with the arrangement shown in figure 5, it would be interesting to know what alternating voltage is applied to the grids, and what is the maximum d-c bias voltage used. Furthermore, I would like to know the reason for using energized grids in connection with the arrangement shown in figure 6. It was my understanding that the ignition of the cathode spot can be fully taken care of by the ignitors as developed at present.

G. R. McDonald: I wish to thank Mr. Jones for pointing out an error in the connections of figure 6, and for the further discussion on the voltage control of ignitron equipments.

Referring to Mr. Marti's comments, we agree that when delaying the firing of the anodes of a rectifier to modify the output voltage, the anode currents and voltages remain essentially the same with the same rectifier output. As pointed out by Mr. Marti, rectification is made more difficult when delaying the firing of the rectifie anodes because the inverse voltage increase more rapidly at the end of an anode con ducting period. This causes some addi tional positive-ion bombardment of th anodes. When rectifier loads are light this is of no consequence. However, if rectifier is operated at its maximum curren rating, and the firing of the anodes is de layed to a considerable extent, this ma become of some consequence and should b given consideration in the application o equipment. The type of flat voltag regulation described by Mr. Marti is natu rally easy for a rectifier as maximur grid control comes at light load.

The equipment chosen for controllin the rectifier grids to modify the outpu voltage is entirely a function of the typ and range of control desired in each case.

Operating Experience With Petersen Coils on 66-Kv System of Metropolitan Edison Company

H. M. RANKIN
MEMBER AIEE

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ASSOCIATE AIEE

THE Metropolitan Edison Company, operating in the eastern part of Pennsylvania, has a total of 264 miles of 66,000-volt overhead transmission lines, to which interconnections at this voltage with the systems of the Pennsylvania Water and Power Company and the Pennsylvania Power and Light Company add 59 miles, making a total of 323 circuit miles. A single-line diagram of this system is shown in figure 1, which also shows the various types of line construction used, ranging from all-wood construction to all-steel construction, with overhead ground wires and counterpoise. The system neutral is grounded through transformers at West Reading, Middletown, and the Holtwood station of the Pennsylvania Water and Power Company. At West Reading, the grounding bank consists of three single-phase 500-kva 66,000-13,200-volt transformers connected wye-delta, and at Middletown a three-phase zigzag transformer having an equivalent transforme capacity of 1,000 kva. At Holtwoo each 66,000-volt line, as shown in figur 1, terminates in a 20,000-kva three-phas 69,000-13,200-volt wye-delta transformer having the high-voltage neutral grounde through a 300-ohm reactor. The 13,200-volt winding of these transformers is connected to the 13,200-volt station bus.

The system is, in general, equippe with conventional induction types o directional-phase and directional-groun relays. The only exception is in th phase relaying on the Pennsylvani Water and Power Company's lines an on the York end of circuits numbe 77 and number 78, where directional distance type relays are used. Balance relay schemes are used where advantage can be gained thereby.

All lightning arresters and trans formers are insulated for full line-to line voltage.

Application and Design

Interruptions were being experience on this system due to insulator flash overs, the majority of which were as cribable to lightning, and various mean of lightning-proofing these lines wer

Paper number 39-37, recommended by the AIEE committees on protective devices and power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 27, 1938; made available for preprinting December 30, 1938.

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The authors wish to express their appreciation of the advice and assistance rendered by E. M. Hunter, of the General Electric Company, in the conducting of tests and in preparation of data.

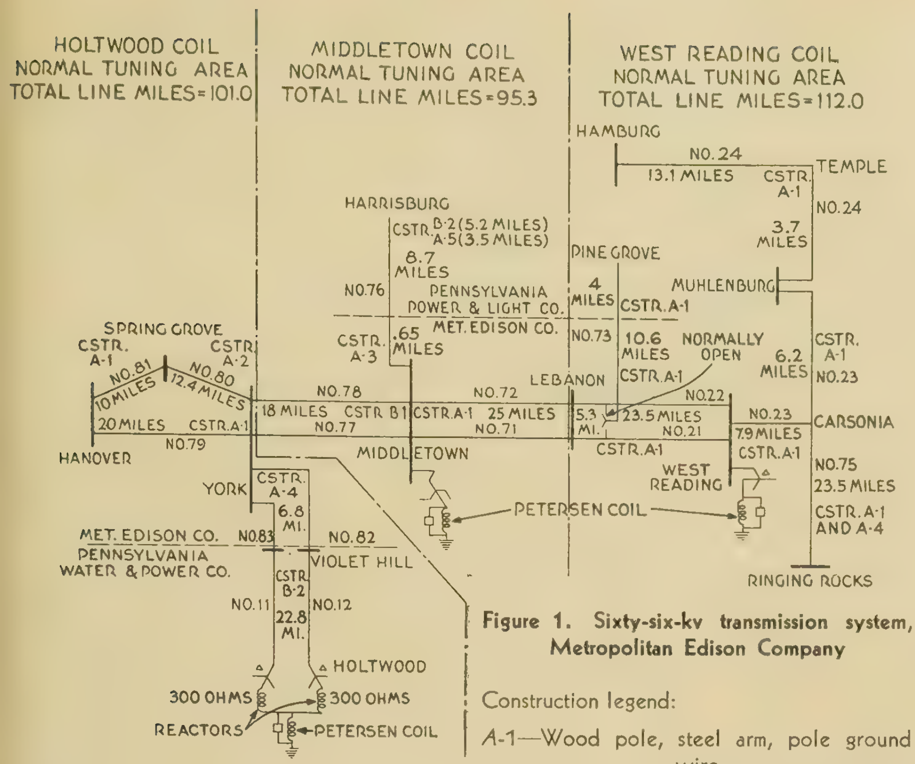

Figure 1. Sixty-six-kv transmission system, Metropolitan Edison Company

Construction legend:

A-1—Wood pole, steel arm, pole ground wire

A-2—Wood pole, steel arm, pole ground wire, overhead ground wire

A-3—Wood pole, steel arm, pole ground wire, overhead ground wire, expulsion gaps

A-4—Wood pole, wood arm, no pole ground wire

A-5—Wood pole, steel arm, pole ground wire, expulsion gaps, counterpoise

B-1—Steel tower, no overhead ground wire

B-2—Steel tower, two overhead ground wires, counterpoise

considered. It was found, however, that approximately 50 miles of double-circuit line, which was originally built for operation at 33,000 volts, could not be changed to accommodate either overhead ground wires or expulsion gaps without complete rebuilding. An analysis of the operation for the year 1936 showed that approximately 69 per cent of the faults were cleared by ground or phase and ground relays. Numerous conductor failures and insulator failures during lightning storms were experienced. It was therefore finally decided to install Petersen coils, or ground fault neutralizers, on this system, with the expectation that over 70 per cent of the outages would be eliminated. Due to operating conditions, and in order to maintain selective relaying under all conditions, it was necessary to install a coil at each location where the system neutral was grounded.

The neutral bushing of the zigzag grounding transformer at Middletown was originally rated at 15 kv. This was replaced with one rated 50 kv, in order to provide insulation capable of handling at least line-to-neutral voltage. No other change of system insulation was necessary on account of the Petersen-coil application.

The three Petersen coils were furnished by the General Electric Company and were designed from measurements of charging current made by disconnecting the grounding transformers at West Reading and Middletown and opening the neutral-ground connections at Holtwood, and grounding one phase of the system through a current transformer. This measurement was made at various locations on the 66,000-volt system. The average charging current per mile of circuit was found to be 0.34 amperes.

In the application of three Petersen coils, it was decided to have each coil normally protect approximately one-third of the total circuit mileage and to have sufficient range to allow for any abnormal operation which could be anticipated.

All of the coils were designed for ten-minute operation, those at West Reading and Middletown having ratings of 15 to 60 amperes, and the Holtwood coil having a rating of 12 to 48 amperes. The minimum current rating was selected to permit operation of the minimum expected circuit mileage and maximum rating selected to allow for system growth. The West Reading and Middletown coils are designed with single bushings, conservator tanks, Thyrite by-pass resistors, current transformers inside the tank in the grounded lead, and external manually-operated ratio adjusters by means of which 25 tap points are obtainable. The Holtwood coil is similar, except that a gas seal is used instead of the conservator.

Each coil was provided with an oil circuit breaker which would automatically short-circuit the coil after current had passed for a predetermined time, thereby solidly grounding the system neutral to allow relaying of the faulty line. Figure 2 shows the Middletown coil and short-circuiting oil circuit breaker as installed, together with the grounding transformer in its original position.

The auxiliary-control apparatus for each coil consists of an instantaneous overcurrent self-reset relay, a timing relay, an operation counter, a signal, and a recording ammeter, connected as shown in figure 3. Operation of the overcurrent relay simultaneously energizes the timing relay and the operation counter. The timing relay is a d-c motor-operated device used to close the short-circuiting oil circuit breaker if current flows through the coil continuously for a period of five seconds. The timing relay returns to the starting position instantly if the fault is cleared within the five-second period. The operation-counter registers the number of operations of the overcurrent relay. This provides a more accurate record of the total number of faults, as several faults occurring within a short space of time would appear as a single fault on the recording ammeter, due to the slow

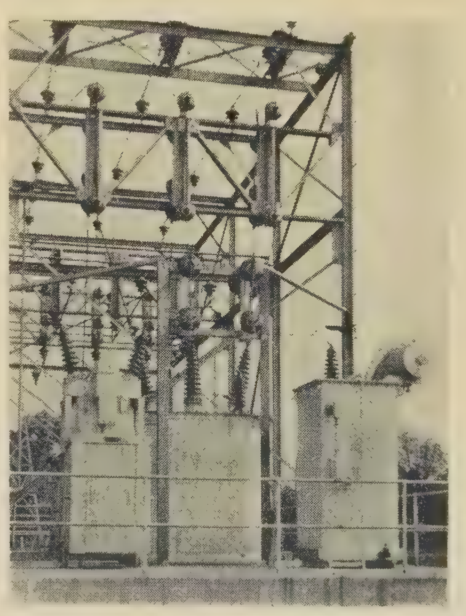

Figure 2. View of Petersen coil and short-circuiting oil circuit breaker installed at Middletown

chart speed. At West Reading and Middletown, the signal is connected as shown in figure 3, and is energized only when the short-circuiting oil circuit breaker closes. At Holtwood, it is connected in parallel with the timing relay, becoming energized directly from the overcurrent relay contact.

Tuning Tests

The coils were installed in September 1937, and immediately after their installation tuning tests were made to determine the proper operating taps for the various operating conditions. Normal tuning areas were established, as shown in figure 1, and each coil was tuned to its respective area without placing a fault on the system, in the manner described in a recent AIEE paper.[1] In these tests it was found that the resonance or "in tune" current values were high, in some cases far exceeding the continuous current rating of the coils. This high resonance current was thought to be due to excessive electrostatic unbalance of the transmission lines, and indicated that transpositions would be necessary. The effect of the various transpositions was calculated and the transpositions made as described recently in an AIEE paper.[2]

Transpositions

Referring to figure 1, circuits numbers 21 and 22 between West Reading and Lebanon, and circuits numbers 71 and 72 between Lebanon and Middletown, were given one complete transposition. The section of circuits numbers 77 and 78 between York Haven and York, which is about two-thirds of their length, was rearranged with phase positions differing from one another and from that section between Middletown and York Haven, so that, in effect, with both circuits in service there is one complete transposition. The bottom and middle phases of circuit number 24 between Temple and Hamburg were interchanged. The two top phases of circuit number 80 between York and Spring Grove were interchanged, and also the two phases on the same side of the pole on circuit number 81 between Spring Grove and Hanover. On circuit number 82, which is a tie line between this company's substation and that of the Pennsylvania Water and Power Company in York, the vertical phasing was changed from *C-B-A* to *B-A-C*. On the Pennsylvania Water and Power Company's system, the top and middle phases of circuit number 11 between York and Holtwood were interchanged. In making these changes, advantage was taken of locations at which the configuration of the circuit changed from vertical to horizontal arrangement, so that the total additional material required for all transpositions consisted of two 14-foot steel crossarms.

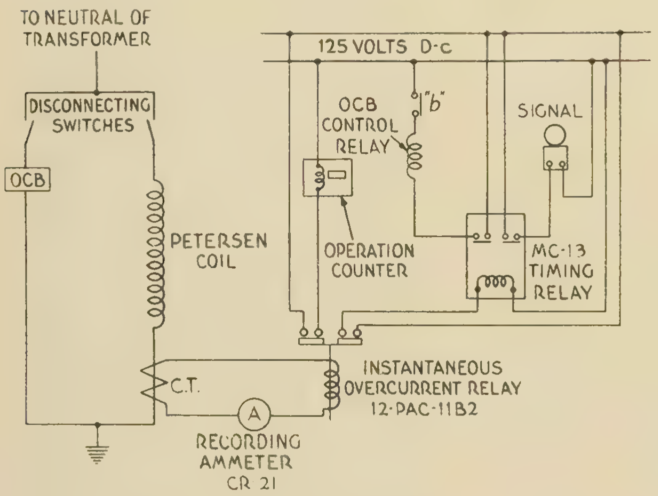

Figure 3. Connections for auxiliary control equipment

The effect of the transpositions was, in some cases, to balance one circuit against another in a given section of the system, which meant that the outage of one circuit in such a section would cause some degree of electrostatic unbalance. If the coils were retuned to the remainder of the system by changing taps, this electrostatic unbalance might be sufficient to cause excessive resonance current to flow through the coils, and a certain amount of detuning would then be necessary. From an operating viewpoint, it was considered preferable to drop the circuit without attempting any retuning, inasmuch as the amount of detuning caused thereby was less than that which was successfully handled in field tests.

After the transpositions, etc., were made, a second set of tuning tests was carried out, and resonance current magnitudes were found to have been reduced to a point which was considered acceptable.

Staged Tests

An automatic oscillograph was then installed at Middletown, and staged tests were conducted. Arcing faults to ground were initiated by connecting a 0.5-ampere fuse wire across a five-unit insulator string suspended from a steel crossarm located on a wood pole, grounded through a down-wire, and energizing this by closing an oil circuit breaker. This down-wire was wrapped several times around the base of the pole, but no attempt was made to lower the pole-footing resistance. A series of seven tests was conducted, six of which were with arcing faults and with system conditions varying from normal tuning to as much as 25 per cent out of tune. The out-of-tune conditions were established by isolating certain lines from that part of the system on which the tests were being conducted, leaving, in some cases, only two coils to clear the fault. No changes of coil taps were made from the normal position for any of the out-of-tune tests. The other test was a solid ground fault, made in order to check the operation of the short-circuiting breakers and the system ground relays. In the arcing-fault tests, the arc was suppressed in each case without any oil-circuit breaker operation or system disturbance

Table I. Petersen Operating Record

	Total	Per Cent of Total	Per Cent of Transient
All faults	190	100.0	
Transient faults	173	91.0	
Transient faults cleared by Petersen coil	128	67.4	74.0

Table II. Cause of Transient Faults Cleared by Petersen Coils

Lightning	84
High wind, rain, sleet, etc.	18
Unknown (fair weather)	18
Bushing flashovers	6
Iron oxide on insulator	1
Broken conductor contacting steel tower	1
Total	128
Per cent of total due to lightning	65.6

1. For all numbered references, see list at end of paper.

the time of extinguishing the arc varying from one-half cycle to seven cycles.

Operating Results

The coils were placed in service October 16, 1937, and in the various tables, "Petersen-Coil Year" designates the year ending October 15, 1938. Table I shows that out of a total of 190 faults occurring during the Petersen-coil year, 128 faults were cleared by the coils without oil-circuit-breaker operation. However, the operation-counters, and the oscillograph which was in service for several months at Middletown, show that actually there were more than 500 arc suppressions. The oscillograms reveal that every fault was not cleared by the coils at the first attempt, as in some cases several arc suppressions occurred in a fraction of a second on the same phase before the fault was completely removed from the system. In these cases, the coils were given credit for clearing only the initial fault, as a system disturbance resulting in oil-circuit-breaker operation would have begun at that time, had the coils not been in service.

As previously stated, it was expected that the coils would remove over 70 per cent of all faults from the system without disturbance. The results show that 67.4 per cent of all faults (74.0 per cent of all transient faults) were cleared by the coils without oil-circuit-breaker operation.

The fact that results have been somewhat lower than was expected, is due, in part, to an unusual condition existing on circuits numbers 71 and 72, where they pass close by an ore-concentrating plant of a large steel company. At this point, under certain weather conditions, the black oxide of iron dust discharged from the stacks deposits on the insulators and reduces their insulation level. Consequently, a single conductor-to-ground fault at another location, with the accompanying increased voltage-to-ground on the two sound phases, caused the dirty insulators to flash over, resulting in simultaneous faults at different locations involving more than one phase. Faults of this nature occurred on several occasions before the cause was determined. Increasing the insulation level in the affected area has apparently remedied this serious condition, as no simultaneous faults have since been experienced; furthermore, to the best of our knowledge, neither has there been a flashover in the vicinity of the concentrating plant.

Table III. Comparison of Faults and Oil-Circuit-Breaker Operations Before and After Petersen-Coil Installation

	Average of Previous Five Years	Petersen-Coil Year
Total faults	164	190
Total transient faults	138	173
Transient faults causing oil-circuit-breaker operation	138	45
Permanent faults	26	17
Total oil-circuit-breaker operations	448	221
Oil-circuit-breaker operations from transient faults	315	118

Table IV. Interruptions Per Circuit

Circuit Number	Number of Circuit Interruptions: Average of Previous Five Years: Lightning	Other Causes	Total	Petersen-Coil Year: Lightning	Other Causes	Total	Construction (Figure 1)	Type of Insulators	Equivalent Spacing (Inches)
11							*B*-2	Suspension	
12							*B*-2	Suspension	
21	10.4	5.4	15.8	7 (1)	3	10 (1)	*A*-1	Pin	45
22	12.8	4.4	17.2	5	9	14	*A*-1	Pin	45
23	1.6	1.2	2.8	0	1	1	*A*-1	Pin and suspension	66
24	1.4	2.2	3.6	5	1	6	*A*-1	Pin	61
71	16.2	8.2	24.4	5	6	11	*A*-1	Pin	60
72	13.2	6.8	20.0	6 (1)	9	15 (1)	*A*-1	Pin	60
75	3.6	3.4	7.0	5 (1)	1	6 (1)	*A*-1 and *A*-4	Pin	54
76							*A*-3, *A*-5, *B*-2	Suspension	
77	6.6	1.6	8.2	0 (1)	0	0 (1)	*B*-1	Suspension	153
78	8.6	2.2	10.8	0	1	1	*B*-1	Suspension	153
79	8.2	5.0	13.2	1	1	2	*A*-1	Suspension	94
80	4.6	3.0	7.6	1	0	1	*A*-2	Suspension	100
81	6.0	4.0	10.0	1	1	2	*A*-1	Suspension	100
82	2.6	1.0	3.6	1	0	1	*A*-4	Suspension	91
83	2.4	1.2	3.6	0	0	0	*A*-4	Suspension	91
Totals	98.2	49.6	147.8	37 (4)	33	70 (4)			
Summary									
Pin-type insulators	57.6	30.4	88.0	33 (3)	29	62 (3)	148 circuit miles		
Suspension insulators	39.0	18.0	57.0	3 (1)	3	6 (1)	147 circuit miles		
Combination pin and suspension	1.6	1.2	2.8	0	1	1	14 circuit miles		

NOTE: Figures in parentheses represent additional interruptions occurring while coils were short circuited and therefore inoperative.

An analysis of the fault-clearing record of the coils is given in table II. It will be noted that 84 of the 128 faults cleared by the coil were caused by lightning. Approximately two-thirds of the 18 operations from unknown causes occurred around daybreak, and were believed to have been caused by birds.

A comparison of the number of faults and oil-circuit-breaker operations before and after the Petersen-coil installation is given in table III. Although the total number of faults during the Petersen-coil year exceeded the five-year average, the total number of oil-circuit-breaker operations was reduced more than 50 per cent. This performance makes it apparent that oil-circuit-breaker maintenance has been materially reduced.

The over-all reduction of system disturbances is clearly shown by the fact that the total number of transient faults causing oil-circuit-breaker operation has been reduced from an average of 138 per year, over five years, to 45 during the Petersen-coil year, or a reduction of 67.4 per cent. Lightning was responsible for 36 of the 45 faults, 29 of which were cleared by phase relays. The majority of these faults, however, occurred in the 50-mile section of double-circuit line with closely spaced conductors. Lightning, therefore, was responsible for a total of 120 transient faults, 70 per cent of which were cleared by the Petersen coils. The reduction in oil-circuit-breaker operations caused by transient faults was about 64 per cent.

It is also interesting to note that during the Petersen-coil year the total number of permanent faults was only about two-thirds of the five-year average (table III). It seems reasonable to credit this reduction to the coil performance, inasmuch as fewer insulator failures and conductor failures were experienced during the coil year than in any one of the previous five years.

The number of interruptions per circuit during the Petersen-coil year, in

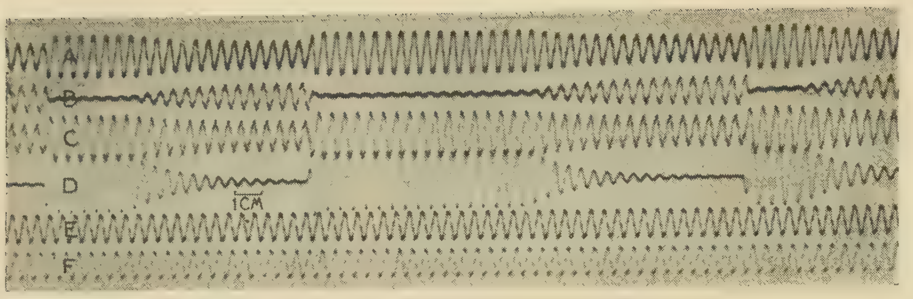

Figure 4. Oscillogram showing fault due to tree contact

A—Line-to-neutral voltage, phase *A*; 1 CM = 60 volts root-mean-square (×600)

B—Line-to-neutral voltage, phase *B*; 1 CM = 60 volts root-mean-square (×600)

C—Line-to-neutral voltage, phase *C*; 1 CM = 60 volts root-mean-square (×600)

D—Petersen-coil current; 1 CM = 12.0 amperes root-mean-square (×1)

E—Line-to-line voltage, phase *A-B*; 1 CM = 100 volts root-mean-square (×600)

F—Line-to-line voltage, phase *B-C*; 1 CM = 100 volts root-mean-square (×600) (measured maximum to maximum)

comparison with the average of the previous five years, is shown in table IV. This record shows that the performance of the circuits having closely-spaced conductors and pin-type insulators has been considerably below expectations. On the other hand, the performance of the circuits with five-unit suspension insulators has been excellent. It is probable that part of the reason for the poor performance of the pin-insulator circuits lies in the high pole-footing resistance, which in the event of surge current passing to ground, would cause the potential of the entire pole structure to be raised above ground potential sufficiently to cause multiple flashovers on the same structure. Spot measurements of pole-footing resistance, recently made along circuits numbers 21 and 22, were found to vary between 25 and 150 ohms, with an average from 20 locations of 80 ohms. Further study is being made of the pin-type circuits, in an effort to improve their operation.

Data on circuits numbers 11 and 12, of the Pennsylvania Water and Power Company's system, have been omitted from table IV because of certain changes in line protection made about two years prior to the coil installation. Likewise, circuit number 76 has been omitted due to its having been in use less than one year prior to the coil installation.

Summarizing the data in table IV, comparing the Petersen-coil year with the average of the previous five years, it will be seen that the reduction of interruptions to pin-type insulator circuits was 27 per cent; of suspension insulator circuits, 88 per cent; and of the combination pin- and suspension-insulator circuits, 64 per cent. Of all circuits, a reduction of 51 per cent was obtained.

On four occasions, during severe lightning storms, the system became separated by a permanent fault between Middletown and West Reading, resulting in two systems protected by Petersen coils. On each occasion, one or more subsequent flashovers were cleared in each section with the normal coil tap. In one case this represented an out-of-tune condition of nearly 30 per cent.

Three rather peculiar instances in which the Petersen coils saved the system from major disturbances may be worthy of reporting:

1. During a high wind storm, a tree growing close to circuit number 71 was blown into the line, causing 41 coil operations within a period of 25 minutes, although no system disturbance of any kind was observed. The evidence was found by the patrol crew on the following day. Figure 4 shows a section of an oscillogram obtained during this fault, showing three of the 41 arcs extinguished in various times during a period of approximately one second.

2. A bushing on an oil circuit breaker connected to the 66-kv bus at West Reading broke down and arced to the ground sleeve inside the oil tank. This resulted in five coil operations within a period of a few seconds. Although at the time of the coil operation puffs of smoke were seen in the 66-kv switching structure by the station operator, the cause of the operations was not located until two days later when the oil circuit breaker was opened for overhauling. Again, there was no disturbance on the system. Figure 5 is a section of an oscillogram of this fault, showing that one of the flashovers cleared in less than one-half cycle.

3. A dead-end loop on a steel tower, which at some previous time had been burned by a flashover, broke during a high wind, and the side having the splicing clamp on the end of the wire swung intermittently into the tower. This resulted in a total of 265 coil operations over a period of one hour. During this time the trouble was located and the faulty section isolated, all of which took place with no disturbance to service or line-to-line voltage. The oscillogram of this fault showed phenomena similar to that which took place during the tree fault (figure 4), although in some instances more frequent contacts occurred.

In figure 6 is shown an oscillogram of a typical "unknown" cause of coil operation which was cleared in approximately $2^1/_2$ cycles. This operation occurred at 5:31 a.m., and was suspected to have been caused by birds.

It is important to note that from numerous oscillograms obtained showing arc suppression by the coils, in no case did the line-to-ground voltage on either of the two sound phases exceed the line-to-line voltage.

Permanent Ground-Fault Relaying and Coil Short-Circuiting Breaker Operation

In order to maintain proper selectivity in the operation of ground relays on

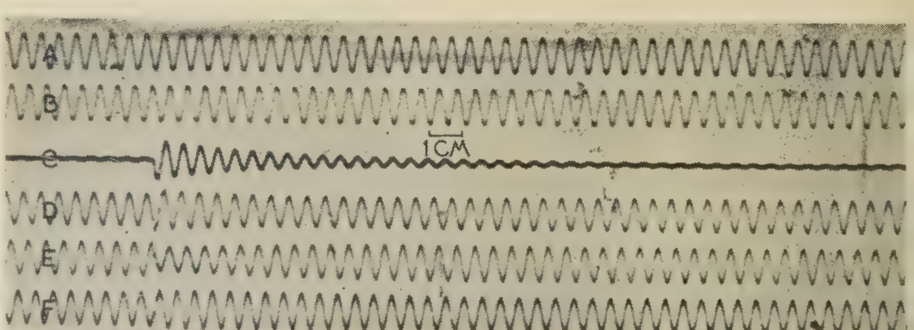

Figure 5. Oscillogram showing fault due to failure of circuit-breaker bushing

A—Line-to-line voltage, phase *A-B*; 1 CM = 100 volts root-mean-square (×600)

B—Line-to-line voltage, phase *B-C*; 1 CM = 100 volts root-mean-square (×600)

C—Petersen-coil current; 1 CM = 6.5 amperes root-mean-square (×1)

D—Line-to-neutral voltage, phase *A*; 1 CM = 60 volts root-mean-square (×600)

E—Line-to-neutral voltage, phase *B*; 1 CM = 60 volts root-mean-square (×600)

F—Line-to-neutral voltage, phase *C*; 1 CM = 60 volts root-mean-square (×600) (measured maximum to maximum)

the system, it was, of course, necessary to take care that the coil short-circuiting breakers at all three locations would close at approximately the same time. The d-c motor-operated timing relays used for this purpose were found to be remarkably consistent, and no particular difficulty was experienced in this respect. All coil breakers are reopened manually on order of the system operator.

Only one incorrect oil-circuit-breaker operation was attributed to dissimilar closing times of the coil breakers. In this case a single conductor-to-ground flashover occurred on circuit number 78, which was not cleared by the coils within the five-second period. The coil breakers then closed automatically and circuit number 78 was relayed at both ends, together with the York end of circuit number 77, all by ground relays. It was found that the Middletown coil breaker was about 30 cycles slower than the Holtwood and West Reading coil breakers, the effect of which was to delay tripping at Middletown long enough to allow tripping of circuit number 77 at York. Incidentally, this was the only case observed where a single conductor-to-ground fault was not cleared within the five-second period. It has been suggested that this time be increased in view of experiences with other installations.[3] This cannot be done at present because of eight-second time-delay no-voltage relays connected to coupling capacitor potential devices at a station fed from circuit number 72.

The ground relays on this system are of the directional and nondirectional overcurrent types, most of which have been in service for nearly 15 years. To date, no changes of any nature have been made to the ground-relay schemes or settings on account of the Petersen-coil installation. The 66-kv system relaying, in general, during the Petersen-coil year has been satisfactory, being on a par with the relaying during the previous years.

In two instances, balanced ground relays have operated along with phase relays to clear double-circuit multiple flashovers-to-ground, although none of the coil breakers had closed before the fault was cleared. Further investigation is being made in an effort to determine the reason for these ground-relay operations.

With multiple-coil operation, it is apparent that if any one coil is short-circuited, all other coils are rendered ineffective, as the system is then solidly grounded. Therefore, it is not only important that all coil breakers close at the same time, but it is also important, in the case of manual reopening, that they be reopened in the proper order; otherwise improper relaying might take place for subsequent faults occurring before all coil breakers have been reopened. On our system, this condition is almost entirely avoided by not reopening Middletown coil breaker until after the other two coil breakers have been reopened.

In some cases, particularly during electrical storms, an appreciable time elapsed before all coil breakers were reopened, during which faults occurred subsequent to the one causing the coil-breaker closures, causing interruptions that might otherwise have been avoided. A total of seven ground faults occurred in such periods during the Petersen-coil year, four of which caused circuit interruptions. Therefore, in order to get all coils back into service as quickly as possible, plans are under consideration to install automatic-reopening equipment on the coil breakers. This equipment will function to reopen the coil breaker after fault current has ceased to flow in the grounding transformer.

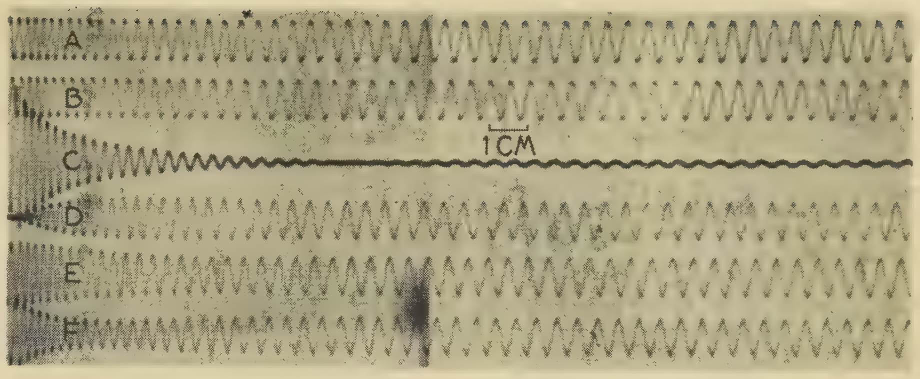

Figure 6. Oscillogram showing flashover from unknown cause

A—Line-to-line voltage, phase *A-B*; 1 CM = 100 volts root-mean-square (×600)

B—Line-to-line voltage, phase *B-C*; 1 CM = 100 volts root-mean-square (×600)

C—Petersen-coil current; 1 CM = 6.5 amperes root-mean-square (×1)

D—Line-to-neutral voltage, phase *A*; 1 CM = 60 volts root-mean-square (×600)

E—Line-to-neutral voltage, phase *B*; 1 CM = 60 volts root-mean-square (×600)

F—Line-to-neutral voltage, phase *C*; 1 CM = 60 volts root-mean-square (×600) (measured maximum to maximum)

Conclusions

From experience obtained thus far, the following appear to be reasonable conclusions:

1. The amount of electrostatic unbalance of a system should first be determined before a coil installation is planned.

2. Experience with multiple-coil installation indicates that the addition of a suitable automatic-reopening device for the short-circuiting oil circuit breakers is desirable.

3. Multiple-coil operation has presented no serious difficulty from the standpoint of system relaying.

4. On lines of fairly modern construction, with five suspension disk insulators, the improvement in service is up to expectations.

5. On lines of older construction, using pin insulators, the improvement is not so marked, indicating, we believe, that more multiple flashovers occur on these lines.

6. It is apparent that successful Petersen-coil operation may be expected with the coils appreciably out of tune. Under abnormal system operation, one section of the system operated with two coils as much as 30 per cent out of tune, during which time successful operations were recorded.

7. Operation of the coils has been responsible for a reduction in the total number of oil-circuit-breaker operations, with a consequent reduction in maintenance cost.

References

1. Some Engineering Features of Petersen Coils and Their Applications, **E. M. Hunter**. AIEE Transactions, volume 57, 1938.

2. The Electrostatic Unbalance of Transmission Lines and Its Effect on the Application of Petersen Coils, **J. A. M. Lyon**. AIEE Transactions, volume 58, 1939, pages 107–11 (March section).

3. Test and Operation of Petersen Coil on 100-Kv System of Public Service Company of Colorado, **W. D. Hardway** and **W. W. Lewis**. AIEE Transactions, volume 56, 1937.

Discussion

E. M. Hunter (General Electric Company, Schenectady, N. Y.): It is worthy of note that the grounded-neutral method of operation has been supplemented by Petersen coils on the Metropolitan Edison Company 66-kv system. This is one of several applications of its kind in the United States and there are indications of a very definite trend in the industry away from the solid neutral ground with its very obvious limitation that every flashover to ground is a short circuit requiring immediate attention. It is believed that the

presentation of the operating experience given in this paper is very timely and should be of considerable value to others who may be considering similar applications.

It is expected that from time to time other companies which are now operating solidly grounded will supplement their ground fault protection with Petersen coils. This requires some system planning because certain insulation levels must be maintained in the connected electrical apparatus. Transformer neutrals should be fully insulated, lightning arresters should be of the ungrounded-neutral type, and interconnections of systems of different voltage levels should be through two-winding transformers and not autotransformers. These facts should be kept in mind when new purchases of electrical apparatus are contemplated because otherwise the cost of rebuilding equipment to make it suitable for the Petersen coils may make the application uneconomical.

E. H. Bancker (General Electric Company, Schenectady, N. Y.): One of the factors brought to light in this paper is the fact that the use of Petersen coils does not upset normal ground relaying. It should be pointed out that this does however require that when the coils are short-circuited, there should be about as many grounded neutrals as existed at the time the relay settings were made, or else the relays will have to be reset for the new ground-fault current condition. This factor must be considered when an installation is proposed in order to determine whether certain neutrals previously grounded and not to be equipped with coils also should be provided with grounding circuit breakers.

The control for automatically reopening the breaker may be arranged to perform in any one of several possible ways. In my opinion the circuit breakers should reopen immediately when the coil current ceases in order that the system shall return to Petersen-coil operation as quickly as possible. It seems to me that the breaker should also reopen after it has been closed a definite time that is long enough to assure relay operation even though the ground current is still flowing, so that in the event of relay failures the system will return to Petersen-coil operation rather than maintain ground-fault current. Of course, this means continuous-rated coils or action by the operators to isolate faulty sections before the coil thermal limits are exceeded. It would seem preferable to operate with the Petersen coils in service and one conductor grounded in the event of relay failure, rather than allow the system to continue to supply ground-fault current until breakers are opened manually. The opinion of others on this point is solicited. It would be helpful to the manufacturers in planning future applications and in the interest of standardization.

H. K. Sels (Public Service Electric and Gas Company, Newark, N. J.): The application of Petersen coils to a system must be studied very carefully to determine if the benefits which may be derived are great enough to justify their installation. Messrs. Rankin and Neidig have reported in table IV the interruptions which have been experienced per circuit over a number of years. This tabulation shows that circuits numbers 21 and 22, 71 and 72 have between 60 and 80 interruptions per 100 miles per year so that the pole ground wire apparently has no beneficial influence on the number of interruptions and in fact may have the opposite effect. However it is evident that the presence of this ground wire does give a preponderance of single-phase faults which presents an ideal situation for the successful operation of the Petersen coil.

In studying a section of our system for the application of the Petersen coil, it was found from an analysis of several hundred oscillograms that approximately 80 per cent of the faults started on more than one phase so that only 20 per cent of the faults remained as single phase for a Petersen coil to clear. It is therefore felt that in order to obtain sufficient improvement in line performance that the successful application of a Petersen coil also required a large item of expense in a general reconstruction of most of the lines to increase the proportion of the single-phase faults. Since in connection with the reconstruction protector tubes could be applied more cheaply than the Petersen coil, it was decided that the Petersen coil should not be installed.

I believe that it would contribute considerably to the paper if Messrs. Rankin and Neidig would submit additional information on the proportion of single-phase faults which they believe occur on their line construction. This should show that the installation of the Petersen coil was justified in their case whereas our analysis casts a reasonable doubt on the over-all gain to be obtained by a Petersen-coil installation.

J. R. North (Commonwealth and Southern Corporation, Jackson, Mich.): This paper is very interesting and shows clearly the results obtained by the use of Petersen coils on this system. It further substantiates our opinion, based upon tests and operating experience, that there are many factors which need to be considered carefully in determining the probable effectiveness of Petersen coils in a given application. These include evaluation of

(*a*). Relative number of single line-to-ground faults versus faults involving two line conductors.

(*b*). Relative number of permanent ground faults versus transitory ground faults.

(*c*). Magnitude of the in-phase component of fault current (due to line resistance, insulator leakage, corona, etc.) and influence of voltage recovery rate.

(*d*). System arrangement—radial, loop, multiple lines, relative location of lines.

(*e*). Dynamic and transient overvoltages as may occur with faults at different locations, and the ability of the system insulation to withstand them.

(*f*). Protective relay scheme and necessity for automatically clearing permanent ground faults.

In this connection it may be of interest to mention two rather detailed studies of extensive transmission systems, one operating at 138 kv and the other at 44 kv. On the 138-kv system, calculations indicate what we consider to be an excessive amount of uncompensated fault current if Petersen coils were to be used and there is as yet no definite evidence available to establish the upper limit of permissible magnitude of such fault current under the expected conditions of recovery voltage. Furthermore, rapid and accurate isolation of permanent ground faults would be difficult since the system is quite extensively interconnected. The relative advantages and limitations of various types of operation were carefully evaluated and it was decided to operate this system with the neutral effectively grounded.

The 44-kv system on the other hand consists essentially of a number of radial star-type units, connected together by single lines which may be operated as independent sections. This system appears to lend itself admirably to the use of Petersen coils.

John A. M. Lyon (The Johns Hopkins University, Baltimore, Md.): The experience with iron oxide depositing on pin-type insulators and the consequent reduction in insulation strength suggests the importance of further study on the effects of soot and other deposits on insulation.

It is important to note that the Petersen-coil protection for that part of the transmission system which had closely spaced conductors with pin-type insulators (low insulation level) has been definitely inferior to the protection afforded to the rest of the system consisting of lines of modern construction. Undoubtedly the high pole-footing resistance was also a factor in the relatively lower degree of protection which was afforded to these closely spaced lines. This condition immediately emphasizes the necessity for the consideration of the likelihood of single line-to-ground faults developing into double line-to-ground faults or line-to-line faults. Unfortunately, sufficient information on this subject is not always available. The usual relay records will of course give the past history of a transmission line by classifying line-to-line faults, and line-to-ground faults, but there is no way of knowing (except through the use of an automatic oscillograph) how many of the first type of faults have developed from the second type. It indicates that increased attention should be placed on the possibility of multiple faults developing from single line-to-ground faults. A consideration of the actual construction of the lines in subsequent installations seems warranted, and judgment should be based on such practical information as the present authors have given.

F. Von Voigtlander (Commonwealth and Southern Corporation, Jackson, Mich.): Messrs. Rankin and Neidig report a rather successful application of Petersen coils under somewhat adverse conditions. The system to which the coils were applied had previously been operated grounded through rather high reactances at several stations. Line conditions no doubt were largely responsible for the number of multiple faults experienced, though this may have been increased somewhat by the high neutral reactances. Since nothing would be gained by totally isolating the neutral, and solidly grounding would not be considered because of the condition of the lines and because of some radial services, the application of Petersen coils became a logical consideration for this system.

The important criterion upon which to base performance expectations of Petersen coils becomes the proportion of transitory single line-to-ground faults to all faults

experienced on the system. On lines of small phase spacing, multiple flashovers would tend to be a rather large proportion of the total, and for such faults the Petersen coils would be of value only in that they would probably tend to limit overvoltages from single line-to-ground faults and thereby to some extent mitigate the probability of second faults occurring, as is brought out by the operating experience cited by the authors.

A reasonable balance to ground is desirable on any transmission system, and it is essential where Petersen coils are involved. This is forcefully demonstrated by this application, in which the unbalance to ground was found to be so great as to overload the Petersen coils during normal system operating conditions.

When more than one Petersen coil must be short-circuited for permanent ground-fault isolation, it is necessary to delay all ground-relay action until short-circuiting of the coils involved has been completed. This requirement is believed to restrict the application of multiple Petersen coils on extensive and complicated networks, particularly where large ground-fault currents may be experienced.

Two instances are mentioned by the authors of balanced ground and phase relays operating to clear multiple ground faults before the Petersen coils had been by-passed. Was this not due to the faults being on different phases at different locations on the system so that residual currents could flow between them large enough to trip balanced ground relays, and even phase relays, thereby clearing the faults before sufficient time had elapsed for the by-passing switches to operate?

H. M. Rankin and **R. E. Neidig:** In closing the discussion, the authors wish to express their appreciation of the interest shown in the subject and the comments brought out in the various discussions.

Referring to the discussion by Mr. Sels, the results of a study of interruptions caused by lightning during the year 1936 showed that 47 per cent were cleared by ground relays only, 13 per cent by both phase and ground relays, 31 per cent by phase relays only, and 9 per cent with no relay indication. This analysis was based purely on relay-target indication, and shows that at least 60 per cent of all interruptions involved ground, with a possibility of a good share of another 9 per cent.

Undoubtedly pole ground wire does assist in confining flashovers to line-to-ground, and therefore allows successful coil operation. However, our experience indicates that even with pole ground wires on pin-insulator circuits, it is essential to have low pole-footing resistance in order to avoid multiphase flashovers. On circuits with higher insulation levels (suspension insulators, wood pole), the pole-footing resistance does not appear to be as important a factor in obtaining successful coil operations. An investigation is now being made to determine the necessary requirements for lowering the pole-footing resistance on the pole lines having pin-type insulators.

Mr. Von Voigtlander has expressed an impression that the 66-kv system had previously been grounded through rather high reactances, which may have been partially responsible for the number of multiple flashovers. Although the interconnection power transformers at Holtwood were grounded through high reactances, it is to be pointed out that the grounding transformers at both Middletown and West Reading were operated with solidly grounded neutrals.

From operating experience prior to the coil installation, it was suspected that the improvement in operation of the circuits having closely spaced conductors would not be so marked, which suspicion has been substantiated by the first year's operating record. However, it is expected that this condition will improve from time to time due to the fact that bad poles, having closely spaced conductors, are replaced with new poles, affording greater spacing.

In Mr. Von Voigtlander's opinion, with multiple-coil installations it is necessary to delay all ground-relay action until all coils involved are short-circuited. The authors wish to emphasize that on the Metropolitan Edison Company system no additional delay was introduced to any ground relays above that which existed prior to the installation of the Petersen coils. In the authors' opinion, the determination of ground-relay settings will, in general, not be affected by the use of Petersen coils. It is agreed, however, that greater precautions are necessary in this respect when the system involved is in the form of a multiple-grounded ring, rather than a straight-line or radial system as was the case of the Metropolitan Edison Company. For a single-coil installation, no difficulty should be experienced in this respect with either type of systems.

In connection with the two instances of balanced ground-relay operations reported, further investigation showed that quite possibly they were due to faults being on different phases of the two circuits on the pole line involved, as was suggested by Mr. Von Voigtlander. However, it is definite that these faults occurred within the confines of the pole line involved, inasmuch as circuit-breaker operation occurred only on these lines, which suggests the probability that the multiple-circuit fault occurred on a common-pole structure.

The authors agree with Mr. Bancker's opinion concerning automatic reopening of coil short-circuiting breakers when neutral current ceases to flow. Whether or not the coil breaker should also reopen automatically after a definite time, even though neutral current is flowing, is open to question We believe the adoption of this function would be governed by the operating policies existing prior to the coil installation, and the desirability of continuing such policies. In our case, a circuit-ground-relay failure throws the responsibility for removing the short circuit from the system back to a relay connected in the neutral of the grounding transformer, which, after a definite time delay, operates a warning signal to the station operator. If the operator is then unable to clear the fault within a definite time, the grounding transformer is then disconnected from the system automatically. It is our impression that the increased cost for a continuously rated Petersen coil would far exceed the cost of the equipment required for the above scheme, which we have found to be desirable protection for all grounding transformers.

The following are unusual instances which have occurred since the original paper was written:

1. Piece of fence wire on conductor near insulator was blown into crossarm by wind. Counters recorded over 600 successful operations (each coil) over period of one hour and 28 minutes. West Reading coil breaker was finally closed manually and faulty line (number 75) tripped successfully upon next contact. Film in oscillograph ran out after 162 arc suppressions.

2. During high wind, foreign object (unknown) on pole structure of numbers 21 and 22 circuits, apparently contacting crossarm, caused 220 arc extinctions within 12 minutes, as taken from the oscillograph. Counter readings indicated an average of 51 operations. After 12 minutes the coil breakers operated, clearing the fault, although evidence from the oscillogram showed that an arc had been extinguished a few cycles after the coil breaker received the impulse to close; otherwise an indefinite number of subsequent coil operations would have occurred. This was only the second instance in about 1$^1/_2$ years' operation which indicated the possible desirability of an increased time delay in closing the circuit breakers short-circuiting the Petersen coils.

Factors Affecting Arc Extinction on a Petersen-Coil System

J. R. EATON
MEMBER AIEE

I. Introduction

THE effectiveness of the Petersen coil in extinguishing line-to-ground arcs on a transmission system is ordinarily attributed to the fact that the current in the arc is kept at a low value. Another factor of perhaps greater importance is that characteristic of the Petersen-coil system which results in a very low rate of rise of recovery voltage across the arc terminals. Although this characteristic has long been recognized,[1,2] the engineering literature would seem to indicate that its importance has not been thoroughly considered. This paper calls attention to the importance of the rate of rise of recovery voltage, presents the circuit theory pertaining to the voltage recovery rate, points out some practical aspects of the Petersen-coil system design, and compares the theory with published records of operation. It is hoped that this analysis will lead to a better understanding of Petersen-coil systems which will permit their more effective use. With continued study of system operating records, it may become possible to predict the effectiveness of a system even before it is built.

II. A-C Arc Characteristics

The characteristics of a-c arcs have been studied very extensively by many investigators, particularly with reference to oil-circuit-breaker operation. As many articles have been written on this subject,[3–5] only those salient points having a direct bearing on Petersen-coil system performance may be mentioned here. Consideration will be given only to unconfined arcs in air, as this is the type ordinarily dealt with on Petersen-coil systems.

The extinction of an a-c arc is almost entirely dependent on certain rapid changes which occur in a short interval of time near the instant of zero current. Previous to the instant of current zero, electrons are emitted in great numbers from a small area on the cathode (known as the cathode spot) in which the current density is approximately 4,000 amperes per square centimeter. This emission is due to the high potential gradient set up by space charges in the arc stream which result in a drop of 20–30 volts

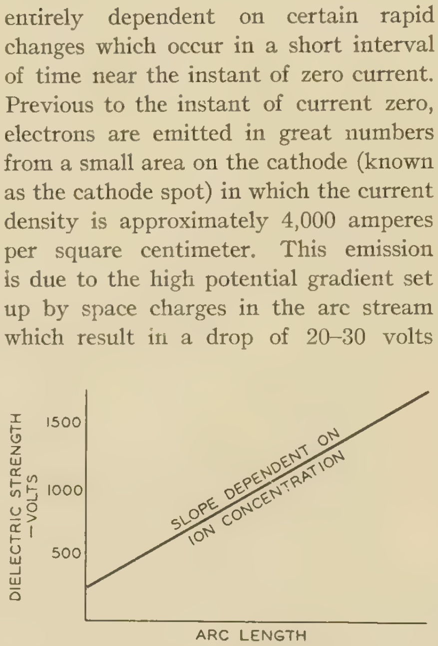

Figure 1. Dielectric strength of arc following current zero

(known as the cathode fall of potential) across a very thin layer (10^{-4} centimeters) at the cathode. The remainder of the arc stream is known as the positive column and is a highly ionized region of approximately equal numbers of electrons and positive ions. The total voltage across the positive column is dependent on its length, being approximately 20 root-mean-square volts per centimeter.

When, due to its cyclic variation, the voltage between the arc electrodes passes through zero, no potential is available in the arc column to move ions toward the electrodes. Consequently arc current and arc voltage pass through zero simultaneously regardless of whether the circuit is predominantly resistive, inductive, or capacitive.

When at a later instant, voltage of reversed polarity appears on the electrodes, the first movement of charges in the discharge space has characteristics quite different from those of the arc. The ions remaining from the previous period of conduction immediately start moving to their respective attracting electrode. However, as no cathode spot exists on the electrode that is now negative, there is at first no copious supply of electrons. The current density is low and the discharge is spread over a considerable area of the cathode. The total current is a very small fraction of the current value which would be noted in an arc. If before the air becomes deionized (from causes to be discussed later) the voltage between electrodes is raised sufficiently to cause a drop in the region of the cathode of 250 volts, a glow discharge will be established. This is a self-maintaining discharge in which large numbers of electrons and positive ions may be generated in the cathode layer. Once the glow discharge is established, the movement of the ions will set up space charges, and the potential gradient at the cathode will increase rapidly, soon resulting in the formation of a cathode spot on the negative terminal. The discharge then becomes an arc. The cathode fall of potential will again be about 20–30 volts while current density will be in excess of 10^3 amperes per square centimeter.

The voltage at which the glow discharge is established is spoken of as the breakdown strength or dielectric strength of the discharge path. This voltage is the sum of two voltages: the 250 volts required at the cathode for the establishment of the glow discharge plus the voltage which must be impressed across the region of the positive column in order to establish this 250 volts at the cathode. The voltage required across the region of the positive column is found to be approximately proportional to the column

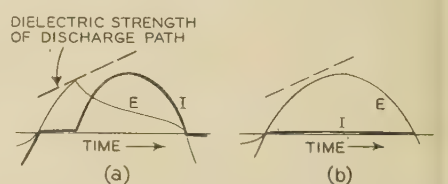

Figure 2. Circuit conditions following the instant of current zero

(*a*) Arc restriking
(*b*) Fails to restrike

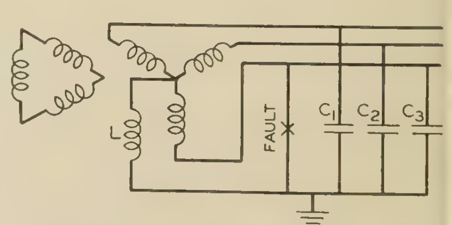

Figure 3. Petersen-coil-system circuit diagram

Paper number **39-64**, recommended by the AIEE committees on power transmission and distribution, and protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 10, 1938; made available for preprinting December 27, 1938.

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The author wishes to acknowledge the assistance of Professor Edward Bennett and Doctor F. A. Maxfield of the University of Wisconsin, who offered many helpful suggestions in the preparation of this paper. Particular credit is due H. R. Hegbar, who assisted very materially in the preparation of the manuscript.

1. For all numbered references, see list at end of paper.

length, the voltage per unit length increasing with decreasing ion density in the region. The dielectric strength of the ionized path is then about as shown in figure 1. Until this voltage is reached, space charges cannot be sufficiently intense to form a cathode spot necessary for the high current of the arc discharge. If following current zero, the voltage applied between electrodes does not exceed the dielectric strength of the path, the arc will not reignite and the path will soon become totally nonconducting. The fault will be cleared.

At all times the path of the discharge is losing ions by the action of the various deionizing agents present. In an oil circuit breaker these deionizing agents are very effective as the arc stream is in close contact with oil and barrier, where turbulence and surface recombination may be very important. In free air the loss of ionization is much slower and is probably due principally to volume recombination and heat loss to the surrounding air. The action of the deionizing agents over the entire cycle must be balanced by the ionizing agents which of course are most active during the conduction period of the cycle. During the period immediately following current zero, ion concentration decreases very rapidly as relatively no ionizing agents are in operation, whereas the deionizing agents are always in effect. This loss of ionization in the arc path results in an increase in the voltage required to establish the condition of the glow discharge. In other words the path gains dielectric strength with time. Circuit conditions following the instant of current zero are shown in an exaggerated form in figure 2.

As shown in figure 2, the arc path becomes almost totally nonconducting for a short period following each current zero. That is, the fault circuit is opened mo-

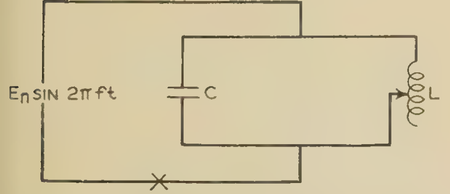

Figure 4. Equivalent circuit of Petersen-coil system (no losses)

mentarily. In order that the arc should be reformed following current zero, the recovery voltage must at some instant exceed the dielectric strength. Obviously a low rate of rise of recovery voltage is favorable to arc extinction. The rate of rise of recovery voltage following current zero is determined by the transient characteristics of the electrical network.

III. System Characteristics

The essential parts of a Petersen-coil system (first approximation) are shown diagrammatically in figure 3. The current through the fault is the vector sum of the current in the capacitances C_1 and C_2 and in the inductance L. By making the inductance of suitable size, the inductive current may be made equal to the capacitive current, with the result that the net fault current is zero. The ground-fault circuit for this network (neglecting losses) is as shown in figure 4, in which C is the sum of C_1, C_2, and C_3, and E_n is the line-to-neutral voltage. With the inductance L adjusted to the correct value such that

$$2\pi fL = \frac{1}{2\pi fC}$$

no current will flow through the fault after conditions have once become stable.

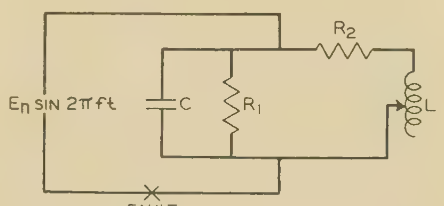

Figure 5. Equivalent circuit of Petersen-coil system (losses considered)

Under this condition the circuit through the fault may be opened without any effect on the circuit behavior. Even with the circuit open, current will continue to oscillate between the capacitance and the inductance at a frequency f, equal to that of E_n. With continued oscillation (zero losses), no voltage will appear across the arc terminals. That is, the rate of rise of recovery voltage is zero, and there is no tendency for an arc to be re-established at the point of fault.

A diagram representing actual conditions more accurately is shown in figure 5. Leakage across insulators, corona loss, etc., are represented by the resistance R_1. Conductor resistance loss and loss in the Petersen coil are represented by R_2. For the purpose of analysis, these losses may all be combined into one resistance and the circuit represented, with a slight change in the value of the inductance, as shown in figure 6. Because of its convenience this circuit will be used in the subsequent discussion.

The root-mean-square value of current in the arc is no longer zero but has some value determined by the magnitude of the system losses. If the circuit through the fault is opened, current will continue to oscillate between the inductance and

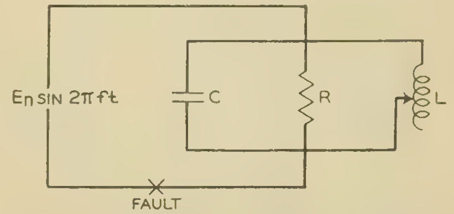

Figure 6. Simplified equivalent circuit of Petersen-coil system (losses considered)

the capacitance, but this oscillation will die down in a finite length of time as the stored energy is absorbed by the resistance R. This transient decrease of amplitude of the oscillation in the RLC branch gives rise to a voltage between the arc terminals, which tends to re-establish the fault.

In section II it was pointed out that following each voltage reversal, the current in an arc remains at substantially zero value for that short length of time required for the voltage between the electrodes to reach a value in excess of the dielectric strength of the discharge path. Hence in the Petersen-coil circuit, each time the current passes through zero, a transient is started in the RLC branch which results in an exponential decrease of the amplitude of the oscillatory voltage appearing on the capacitor. A voltage equal to the difference between the instantaneous value of neutral voltage and the instantaneous value of the capacitor voltage appears across the terminals of the fault, and tends to re-establish the arc. In a circuit such as is shown in figure 6, the current through the fault passes through zero at the instant that the voltage on the capacitor is zero. Until the arc restrikes, the voltage of the capacitor is[6] for the simplified case, figure 6,

$$e_c = E_n\epsilon^{-t/(2RC)}\sin 2\pi ft \tag{1}$$

The recovery voltage across the fault terminals tending to cause the arc to restrike is

$$\begin{aligned} e_r &= [E_n \sin 2\pi ft] - [E_n\epsilon^{-t/(2RC)}\sin 2\pi ft] \\ &= E_n[1 - \epsilon^{-t/(2RC)}]\sin 2\pi ft \end{aligned} \tag{2}$$

as is illustrated in figure 7.

The rate of rise of the *amplitude* of recovery voltage is

$$\frac{dE_r}{dt} = \frac{1}{2\,RC}E_n\epsilon^{-t/(2RC)} \tag{3}$$

which has an initial value ($t = 0$) of

$$\frac{dE_{r0}}{dt} = \frac{1}{2RC}E_n \tag{4}$$

Obviously, the greater the loss in the resonant circuit (as indicated by a low value of R), the faster is the initial rise of the amplitude of recovery voltage and the greater is the posssibility of the arc restriking. Hence, where possible in the design of a Petersen-coil system, the layout should be such as will minimize the losses in the oscillatory branch. This will be discussed in more detail in a later section.

Next, consider the recovery voltage conditions in a simplified Petersen-coil system (no losses considered) in which the coil current does not exactly balance out the capacitance current. That is, the system is not in tune, or

$$\frac{1}{2\pi fC} \neq 2\pi fL_1$$

Referring to figure 4, it may be observed that (considering zero loss) the current through the fault is either inductive or capacitive, depending on whether the coil inductance is less or greater than the "in tune" value. If the circuit is opened at the fault, current will continue to oscillate between the capacitance and the inductance, but the frequency of this oscillation is different from that of the power supply E_n. The oscillatory frequency will be

$$f_1 = \frac{1}{2\pi\sqrt{L_1C}}$$

If the fault circuit is opened at the instant of zero current (which now occurs at the instant of maximum voltage on the capacitor) and no losses in the oscillatory circuit are considered, the transient voltage on the capacitor will be

$$e_c' = E_n \cos 2\pi f_1 t$$

and the recovery voltage across the fault terminals will be

$$e_r = E_n(\cos 2\pi ft - \cos 2\pi f_1 t)$$

The circuit conditions for this case are shown diagrammatically in figure 8. With this type of circuit, if f and f_1 are not greatly different, the initial rate of rise of recovery voltage is quite low, in fact much lower than would be observed on a circuit of the types shown in figure 9 in which the voltage E_n and the fault current I are of corresponding value. This explanation undoubtedly accounts for the fact that inductive or capacitive arcs of considerable current value may be extinguished on a Petersen-coil system whereas arcs of similar current magnitude on other types of circuits (for instance, on an isolated-neutral system) might be very stubborn.

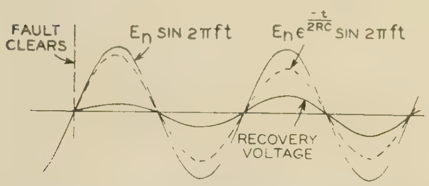

Figure 7. Recovery voltage across arc terminals, Petersen coil in tune (losses considered)

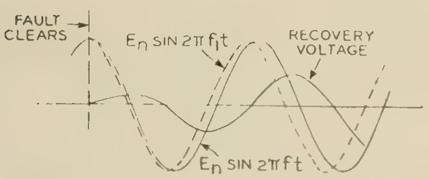

Figure 8. Recovery voltage across arc terminals, Petersen coil inductance less than "in tune" value (no losses considered)

As in the case of the "in tune" condition, the "out of tune" condition is more accurately represented by figure 6 in which losses in the oscillatory circuit are considered. In this case the recovery voltage following the extinction of the arc at current zero is dependent on both the frequency difference and the damping of the transient oscillation. In practical Petersen-coil systems, it may be shown that if the inductance is adjusted to within ten per cent of the "in tune" value the initial rate of rise of the amplitude of recovery voltage is principally governed by the exponential decrease of the transient oscillation. Hence in any correctly adjusted Petersen-coil system it is desirable to minimize the losses in the oscillatory circuit.

If the arc occasioned by a line-to-ground fault is stretched out (as by the wind) before extinction, a considerable voltage may be present across the arc column. Because this voltage is usually nonsinusoidal, a rigorous treatment of the effect of this arc voltage on the circuit behavior becomes quite difficult. However, the conditions may be represented at least approximately by figure 10, in which R' represents the arc. The recovery voltage of this circuit when tuned to resonance is

$$e_r = E_n \sin 2\pi ft\left[1 - \frac{R}{R' + R}\epsilon^{-t/(2RC)}\right]$$

Again it is apparent that the amplitude of recovery voltage (particularly for the in-tune condition) is closely associated with the losses in the resonant circuit and decreases with decreasing losses.

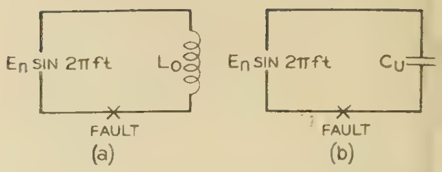

Figure 9. Circuits with steady-state characteristics equivalent to that of a Petersen-coil system which is not correctly compensated

(a) Overcompensated
(b) Undercompensated

As pointed out in section II, the root mean-square voltage across a stable arc in free air is about 20 volts per centimeter (plus 10–20 volts cathode fall) over a considerable range of current magnitude. This voltage apparently is necessary in order to maintain the positive column even in the presence of a cathode spot. If the arc length is increased by the wind,[7] an increasing voltage must be applied to the arc terminals to maintain the positive column. This might be approximated in figure 10 by a steadily increasing value of R'. Consideration of this circuit will show that it is impossible to increase the voltage across R' at a rate greater than that called for by equation 2. Hence arc extinction may occur if the voltage required by the positive column increases faster than the voltage which the circuit can deliver.

From the above discussion, it follows that arc extinction may result from two causes: (1) Following current zero, the recovery voltage across the arc terminals fails to reach a value equal to the dielectric strength of the arc path; (2) the arc length is increased by the wind (or otherwise) at such a rate that the voltage required to maintain the positive column of the arc increases at a rate greater than that which can be supplied by the circuit. Actually arc extinction in the Petersen-coil system probably results from a combination of both causes.

IV. Conditions Favorable to Rapid Arc Extinction

In the foregoing discussion it has been shown that the principal factors favoring rapid arc extinction are:

1. The capacitance current should be closely balanced by the Petersen-coil current. The current in the arc is then a minimum as far as reactive components are concerned, and the frequency of the resonant circuit is that of the power system.

2. The losses in the resonant circuit should be made as small as possible by proper design (to be discussed later). The in-phase component of the current in the arc will then be a minimum.

3. The damping of the oscillation in the

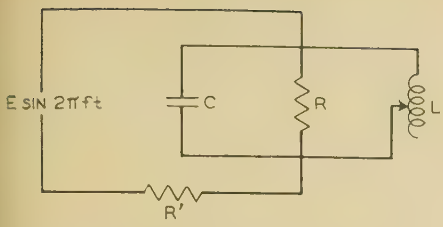

Figure 10. Approximate equivalent of Petersen-coil system, arc resistance considered

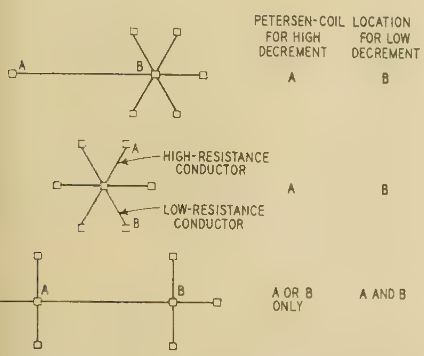

Figure 11. System arrangements in which coil location will affect rate of recovery voltage rise

resonant circuit should be low, thereby minimizing the rate of rise of recovery voltage.

4. The arc length should be as long as possible, and the gap across insulators or arcing horns should be so arranged that the arc length may be readily increased by air currents.

V. Control of Factors Which Govern Arc Extinction

1. Previous articles on Petersen-coil systems have discussed at considerable length the necessity and methods for the accurate control of the tuning of the Petersen coil at all times. While accurate tuning of the coil is probably the most important factor of all, the previous treatments of the subject make further discussion here unnecessary.

2. The control of the resonant circuit losses is a subject which has received practically no treatment in previous discussions. Within certain limits, it may be minimized by proper attention to this circuit. Champe and Von Voigtlander[8] have shown the method for setting up the zero-sequence circuits for a Petersen-coil system of any circuit arrangement. This zero-sequence circuit is of course the resonant circuit whose behavior under transient conditions has such a great effect on the rate of rise of recovery voltage. In setting up this circuit for the purpose of studying the rate of decay of the transient oscillation, it is necessary to include the resistive components of the circuit impedances, and where appreciable, the conductance due to corona losses and insulator leakage. An analysis of the transient characteristics of the equivalent circuit (figure 6) will then clearly demonstrate that the exponent of the damping factor,

$$-\frac{t}{2RC}$$

will in some cases be greatly affected by Petersen-coil location alone. Examples of typical system arrangements in which coil location will be of considerable importance are shown in figure 11.

In general, it may be stated that for best operation, each Petersen coil should be located as near as possible to the capacitance which it compensates, and the connection between the coil and the capacitance should be made through a line having low conductor loss. While practical considerations may in many cases dictate the location of the Petersen coils it must be borne in mind that arc extinction will be much more certain if the coils are so located that the resonant circuit losses are as low as possible. This of course is in direct contradiction to the claims of some writers who have stated that the choice of the coil location is merely a matter of convenience. Fortunately, the desire to locate the coils at points where there would be the least chance of disconnection by switching operations, has in most cases resulted in the installation of the coils at the capacitance centers.

It is of course obvious that the coils themselves and the star-delta transformers connecting the coils to the system should be designed for low loss, inasmuch as they form part of the resonant circuit.

3. The rate of decay of the oscillation in the resonant circuit will in general be determined by the circuit constants and the Petersen-coil locations. Artificial control of this rate of decay may, theoretically at least, be accomplished by the addition of low-loss capacitors across the terminals of the Petersen coil as shown in figure 12. The coil must then be tuned to balance the equivalent capacitance

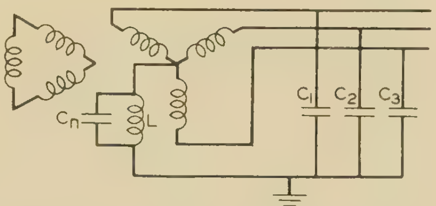

Figure 12. Capacitor added across Petersen-coil terminals to increase stored energy in resonant circuit

$$C_e = C_n + C_1 + C_2 + C_3$$

Ignoring the loss in the capacitance C_n and the possible increase of loss in the Petersen coil itself, the exponent of the damping factor $-t/(2RC)$ (of the simplified circuit, figure 6) will decrease inversely as the equivalent capacitance C_e is increased. To reduce the exponent to 50 per cent of its original value would require the installation of approximately three microfarads per 100 miles of overhead line. The installation of capacitors for this purpose might be quite practicable, particularly on low-voltage circuits. Their use might be well justified on circuits having limited physical clearance on which ground fault arcs must be cleared quickly before they have opportunity to spread to the other conductors.

4. The length of the arc path, as has been shown, affects to a great extent the extinction characteristics. In fact it can be stated that the extinction of arcs on a given system probably occurs when the arc is increased by the wind or otherwise to some fairly definite length dependent on the recovery characteristics of the system. It would be expected that extinction would be much more likely on an overinsulated system than on an underinsulated system because of the difference in the striking distance across insulators. On systems already in operation, there is probably but little chance of improving the extinction characteristics by control of the arc length without major line changes. However, on new construction or in rebuilding, attention should be given to provide a design in which the arc may be stretched out by the wind with a minimum chance of producing line-to-line faults.

VI. Comparison of Theory With Operating Experience

The story of the recovery voltage on a Petersen-coil system is shown quite strikingly at the end of every oscillographic record of a line-to-ground fault.[2,9,10] As predicted by theory, normal conditions are not restored at the instant the fault is cleared, but are brought about through a transient. The exponential character of this transient is effectively demonstrated by plotting on semilogarithmic paper the oscillograph deflection of successive crests of Petersen-coil

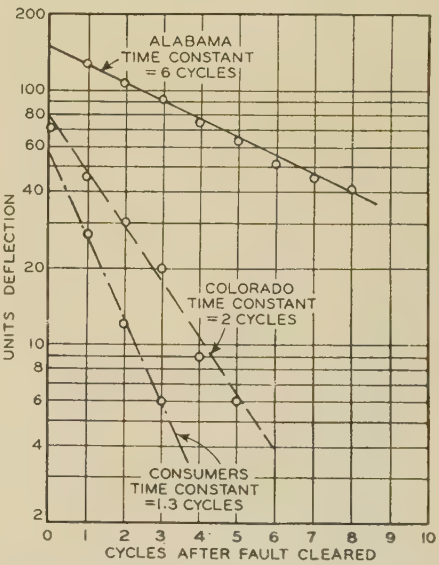

Figure 13. Damping of the oscillation at the end of a line-to-ground fault

current or neutral voltage. Figure 13 shows the straight-line characteristics of the logarithmic plot of Petersen-coil current from published data obtained from the Consumers Power Company* and the Alabama Power Company,† and a

* Figure 12 of reference 9.

† Figure 29 of reference 2.

similar plot of the displacement from normal of the line-to-ground voltage from the Public Service Company of Colorado.*

Considerable information relative to the characteristics of the various systems may be computed from figure 13. The time constant of an exponentially decreasing function is defined as the time required for that function to diminish to $1/\epsilon$ or

Figure 14. Resonant circuit in which residual voltage operates

37 per cent of its original value. For the circuit of figure 6, the time constant may be shown to be

$$\text{time constant} = 2RC \tag{5}$$

The time constants for the three systems have been determined from figure 13, and are shown in table I. Dividing the system neutral voltage by the time constant gives the initial rate of rise of the amplitude of recovery voltage (equation 4). It may be noted that this initial rate of rise is quite different for the three systems investigated.

From equation 5 and the known values of the line-to-ground capacitance of the overhead lines, the value of R has been computed. From this, the in-phase component of fault current is at once determined. For comparison, the computed value and the measured value of fault current are shown in table I. The close agreement between these values seems to support the theory presented. The low value of the equivalent resistance R as calculated for the Consumers Power Company and for the Colorado system probably was due to excessive losses resulting from corona occurring with one conductor grounded.

From table I it may be noted that the initial rate of increase of the amplitude of recovery voltage is quite different on the Consumers Power system than on the Alabama Power system (3,700 kv per second as compared to 255 kv per second). From theory it would seem that for similar arc extinction characteristics, arc length and the rate of voltage recovery are associated in a relation of approximately linear character. Even when considering the difference in insulator clearance provided at 140 kv as compared to that at 44 kv, it appears that the extinction characteristics of the two systems might be quite different on the basis of voltage recovery alone. In addition the Alabama system had the advantage of low arc current, 2.5 amperes, as compared to 40 to 50 amperes on the Consumers Power Company system. It is of interest to note that on the Consumers Power system, arcs established by lightning frequently continued for several seconds even under the action of the severe air currents which ordinarily accompany storms. On the Alabama system, even during staged tests, the period of the arc was very short. In actual operation,[11] numerous disturbances were reported which were too short in duration to give complete fault records. It is possible that these were faults which were extinguished at the first current zero.

The circuit-recovery-voltage characteristics of a Petersen-coil system may be determined with a considerable degree of accuracy from the tuning curve obtained with no grounds on the system, provided however that corona is not excessive under the condition of one conductor grounded. Tuning curves obtained from normal operation require that the unbalance of transpositions result in a slight residual voltage in the system. It may be considered that this residual voltage operates in a series circuit as shown in figure 14. If the inductance is varied, the current will vary reaching a maximum when the circuit is in tune, exactly as is noted on the Petersen-coil system. From this curve alone, the value of R may be determined by straightforward calculations. Considering the transient characteristics of this circuit, it may be shown that the time constant is $2L/R_s$ from which the value of α and the other factors shown in table I may be determined. Using the tuning curve of the Colorado system, the time constant is calculated to be 0.0536, the initial rate of rise of the amplitude of recovery voltage (system in tune) to be 1,150 kv per second, and the fault current to be 12.5 amperes. Here again we may observe a fair comparison between the values of these important quantities determined by quite different methods. Corona occurring during the ground-fault condition which cannot be considered by this approach, would tend to increase both arc current and the rate of rise of recovery voltage.

VII. Conclusions

1. The extinction of arcs on Petersen-coil systems is dependent on the magnitude of the current in the arc, the rate of rise of the recovery voltage across the arc, and the arc length.

2. The rate of rise of the recovery voltage is dependent on the accuracy of tuning and on the transient characteristics of the resonant circuit.

3. The transient characteristics of the resonant circuit are under limited control by the system layout, particularly as regards the location of the Petersen coil. A method for altering the transient characteristics by the use of capacitors is suggested.

4. The magnitude of the uncompensated fault current and the rate of increase of the amplitude of the recovery voltage may be accurately calculated from the known line constants.

5. Satisfactory operation has been reported from Petersen-coil systems having considerably different rates of rise of recovery voltage, consideration being given to differences in arc length for systems operating at different voltages. A further study of system operation may establish the limits in which operation will be successful.

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6. Electric Discharges Waves and Impulse

Table I. Characteristics of Three Petersen-Coil Systems

	Consumers Power Company	Public Service Company of Colorado	Alabama Power Company
Time constant: Cycles	1.3	2.0	6.0
Seconds	0.022	0.033	0.10
1/(time constant)(α)	45	30	10
System voltage, kilovolts	140	100	44
Neutral voltage, kilovolts	81	58	25.5
Initial rate of increase of amplitude of recovery voltage (αE_n), kilovolts per second	3,700	1,740	255
Voltage across arc terminals at the end of one-fourth cycle, kilovolts	13.9	6.8	1.04
Capacitance of resonant circuit (from published data), microfarads	6.80	5.65	2.7
$R = 1/(2\alpha C)$ ohms	1,620	2,930	18,500
In-phase arc current: Calculated E_n/R	50	19.5	1.4
Measured	40–50	22	2.5

* Figure 14 of reference 10.

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Discussion

E. M. Hunter (General Electric Company, Schenectady, N. Y.): In connection with the theory of the arc-quenching properties of the Petersen coil, Mr. Eaton shows that the time constant of the system recovery voltage can be used as a criterion of the performance to be expected. This time constant is equal to twice the product of the resistance and the capacitance in the zero-sequence circuit of the system, and to improve arc extinguishing, this time constant should be lengthened. Mr. Eaton suggests that this may be done by decreasing the losses in the circuit. A low-loss circuit will improve he Petersen-coil performance with regard to quenching ground faults, but may complicate matters during normal operation. On every three-phase system there is some slight unbalance in the three-phase voltages to ground so that there is some residual voltage between the neutrals of the system and ground. In normal operation the Petersen-coil reactance and the zero-sequence capacity reactance of the system form a series resonant circuit, and consequently, the installation of a Petersen coil in the system neutral amplifies the residual voltage. The increase in voltage at the neutral depends upon the same losses in the circuits which were previously mentioned. A low-loss circuit means a high residual voltage. To decrease this residual voltage, the system must be balanced electrostatically. On a low-loss circuit it may be a considerable problem to reduce the normal residual voltage to a level considered suitable for the system. All of this is to indicate that there are practical limits beyond which it may be undesirable to attempt to reduce the losses.

Mr. Eaton also concludes that the best results are obtained from Petersen coils when they are located as close as possible to the capacitance which they are compensating. A very practical significance of this which undoubtedly Mr. Eaton recognizes but which was not brought out in his paper is this. In protecting a given system with Petersen coils, a multiplicity of small coils properly located in the system will give a better performance than one large coil for the entire system. This principle was carried out on the Metropolitan Edison Company's application when three coils were installed. The operating experiences reported by Messrs. Rankin and Neidig, both with the system connected together and divided, justifies the larger number of coils.

The necessity of paralleling the Petersen coil with a low-loss capacitor to increase the stored energy in the circuit, as commented on in Mr. Eaton's paper, on every installation is questionable, but undoubtedly on some circuits where the losses of the circuit may be high due to corona or other causes, this additional shunting capacitor might be of some benefit. This capacitance might also be located on the high-voltage terminals of the transformer in the neutral of which the Petersen coil was located. In this location the capacitors would be so proportioned that they would balance the system electrostatically and thus overcome the previously mentioned objection of the low-loss circuit.

J. R. North and **F. Von Voigtlander** (both of Commonwealth and Southern Corporation, Jackson, Mich.): Mr. Eaton is to be complimented on his very clear exposition of the phenomena involved in the extinction of an unconfined a-c arc in air and for directing attention to the importance of rates of voltage recovery rise and their bearing on successful Petersen-coil operation, particularly on systems where appreciable magnitudes of in-phase components of fault current are involved.

The author suggests shunting the Petersen coil by low-loss capacitors to control the damping factor of the Petersen-coil system. It would appear that the size of such capacitors would be quite large as compared to the capacitance of the lines and might necessitate a considerable increase in the size of the Petersen coil necessary to balance the system capacitance together with this added capacitance.

From time to time tests have been considered to determine the upper limit of magnitude of the in-phase component of ground-fault current that can be successfully extinguished by the use of Petersen coils on various systems. The author points out clearly that not only is the magnitude of such currents an important factor, but the differences in recovery rates of various systems would probably widely affect the maximum currents that could be successfully handled.

Referring to conclusion number 5, it should not be overlooked that there are a number of other factors besides recovery voltage which must also be given careful consideration in analyzing the possible application or performance of Petersen coils on a given system. These include such factors as the magnitude of dynamic voltages experienced on nonfaulted phases, system operating voltage with respect to the corona limit, system network complexity, and required isolation of permanent faults.

A. U. Welch (General Electric Company, Pittsfield, Mass.): Mr. Eaton's statement that "Peterson coils and associated grounding transformers should be designed for low loss" might give the impression that special designs with abnormally low loss would improve the performance by lowering the voltage recovery rate.

However, the I^2R loss in a transmission line and its ground return when supplying charging current to ground is of the same order of magnitude and often higher than the loss in a normal-design Petersen coil. Furthermore, particularly in high-voltage systems, the corona loss with a ground fault is generally much higher than the resistance loss in lines, ground, and Petersen coil. Therefore, reducing the loss in the Petersen coil reduces the total loss only slightly and has negligible effect upon recovery voltage.

J. R. Eaton: Mr. Welch points out that it may be impracticable to reduce the losses in the resonant circuit beyond a certain point. Further attempts to decrease this loss may result in an increased cost entirely out of proportion to the advantage obtained. As mentioned by Mr. Hunter, a reduction of these losses will result in an increase in the residual voltage on the system during normal operating conditions. Hence we find here, as in almost all engineering problems, that we must give consideration to all factors involved and choose as our solution that value which will give the best over-all operation.

Mr. Hunter points out that if capacitors were used to increase the stored energy in the resonant circuit, they might be located at the line terminals of the grounding transformer bank. Although this connection would be satisfactory from the standpoint of system-recovery-voltage characteristics, it may be observed that capacitors so connected would be at all times subjected to full line-to-ground potential. If connected to the system neutral, as suggested in the paper, the capacitors would be subjected to line-to-neutral voltage only during the fault period.

The comments of Mr. North and Mr. Von Voigtlander are of importance in that they call attention to the necessity of a broad consideration of the problem before justifying an installation of Petersen coils on a system. The present paper treats only the factors affecting arc extinction. This of course is only one of the many problems which must be considered in Petersen-coil application.

Induced Current in Parallel Circuits and Its Effect Upon Relays

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MUTUAL INDUCTION is very much like friction in that it is a great blessing under certain conditions and an unmitigated nuisance under others. If it were not for mutual induction there would be no a-c systems as we know them today because there would be no transformers. It is apparent, therefore, that mutual induction is the basis of an entire industry that could not exist without it. On the other hand it has been the bane of the communication industry where induction between circuits is highly undesirable. There is a story told about one of the early long-distance open-wire telephone circuits having several parallel lines in which the experimenter at one end spoke to the man at the other end and asked "Do you hear me?" The reply was, "Perfectly." The first man then asked the second, "Which line am I on?" and after a moment's hesitation the second replied, "All of them." These two illustrations show that mutual induction may be either a blessing or a curse depending upon the circumstances under which it exists.

In three-phase power systems, mutual induction is usually negligible for one or both of the following reasons. In a great majority of installations, the spacing between conductors is so small in comparison with the spacing between circuits that comparatively few of the lines of flux produced by one circuit link the other. The second reason is the presence of transpositions which are commonly employed to balance circuits and to minimize unbalances between circuits caused by mutual induction. If each conductor of each three-phase circuit occupies each of its nine possible positions with respect to each conductor of any other adjacent three-phase circuit for one-ninth of its length, the net effect of one balanced three-phase circuit upon another will be zero. Transpositions of transmission line conductors are normally worked out to approach this ideal quite closely.

The flux field generated by zero-sequence (or ground) current, that is, current flowing out over one or more conductors and returning through the earth, is quite different from that existing in a balanced three-phase system. In this case the circuit spacing of two parallel circuits on the same towers or the same right-of-way may be small in comparison with the conductor spacing since the conductors are the wires of the transmission line and the path of the current in the earth. Furthermore, no transpositions are possible. As a result the mutual inductance between circuits carrying zero-sequence current may vary from zero for widely separated circuits to perhaps 75 per cent of the self-inductance in the case of a double-circuit tower line. A mutual inductance of the order of 50 per cent or greater is far from negligible in determining the distribution of flow of current in the parallel circuits, as will be shown.

To most people resistance and self-inductance are readily understandable terms, but mutual inductance being less commonly encountered seems to be a little more difficult to comprehend. Perhaps the explanation which made its nature clear to the author will be helpful to others, that is, an exact definition of what mutual inductance is. Mutual inductance is the flux linkage with one circuit per ampere in another. Let us examine this statement as applied to a transmission line conductor and see what it says. Everyone knows that self-inductance is the flux linkage per ampere in the circuit itself. In other words, it is the total number of flux lines surrounding the current of one ampere flowing in the circuit. Returning now to mutual inductance, it will be seen that it is merely the number of flux lines around a conductor that results from a flow of one ampere in some other conductor. These statements apply to straight cylindrical conductors, such as transmission lines. When the conductor is in the form of a coil, such as in a transformer, the flux linkage is the sum of all of the flux lines times all of the turns through which they go, when a current of one ampere is flowing.

The transition from mutual inductance to mutual reactance is now very easy to see because reactance is merely the time rate of change of flux linkages per ampere. In other words having established a mutual inductance it is merely necessary to multiply it by $2\pi f$ to obtain the mutual reactance.

There is also present some mutual resistance because the return circuit for both the inducing and the induced currents is in the earth which has some resistance. Accordingly, two adjacent circuits have a mutual impedance containing both a resistance and a reactance term. The formula for calculating the mutual zero-sequence impedance between transmission circuits carrying zero-sequence current will be found in any standard reference book on transmission line calculation[1] and is

$$Z_{m0} = 0.00477f + j0.01397f \log_{10} \frac{D_e}{\text{G.M.D.}}$$

ohms per mile per phase

where f is the frequency, D_e is the distance between the equivalent conductor and its image, and G.M.D. is the N^2 root of the product of the N^2 possible distances between the N conductors of each of the two circuits. For three-phase circuits this becomes the ninth root of the product of nine possible distances.

Where there are many circuits to be calculated, much time may be saved by referring to figures 77 and 82 of the book "Symmetrical Components" by C. M. Wagner and R. D. Evans. From these figures the equivalent depth to the image and the mutual zero-sequence reactance may be obtained directly, and the only computation necessary is the derivation of the geometric mean distance between the conductors designated as G.M.D.

Whenever two circuits having mutual zero-sequence reactance are electrically connected at one or both ends, an equivalent circuit may be drawn showing both the self and mutual zero-sequence impedances,[2] and this will be found most convenient in setting up the impedance diagram for the purpose of calculating the flow of zero-sequence current. Where the lines are bussed at both ends and a fault exists along the length of one of the circuits, two equivalent impedances may be set up, each representing the network in one direction from the point of fault and the two equivalent networks connected together and reduced by the well-known wye-delta method or set up on a calculating table. See figure 1*b*.

The effect of mutual reactance between circuits is to cause circulating current, or a

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Paper number 39-30, recommended by the AIEE committee on protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 26, 1938; made available for preprinting December 19, 1938.

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1. For all numbered references, see list at end of paper.

redistribution of the ground fault current as calculated without the effect of mutual zero-sequence impedance. The first evidence of this probably manifested itself in connection with directional ground relays where an inexplicable lack of selectivity appeared. Some years ago, before the methods of calculating the flow of zero-sequence current were as well known as they now are, cases would frequently arise in which there was an apparent loss of selectivity between directional ground-current relays that, according to calculations, should have had plenty of time interval between them. In the light of subsequent knowledge it is easy to understand how the time interval became too small to retain selective action.

As the use of directional ground-current relays on parallel circuits is still very common, it may be worth while to show why it is that a fault on one of the lines will

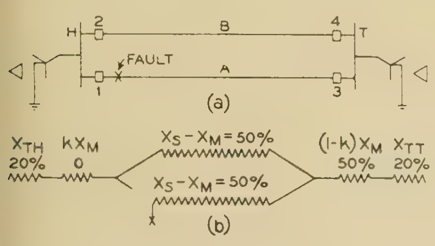

Figure 1

(*a*) One-line diagram of parallel line system

(*b*) Equivalent zero-sequence reactance diagram for ground fault after breaker number 1 opens

X_{TH}—Zero-sequence reactance external to H
X_{TT}—Zero-sequence reactance external to T
X_S—Zero-sequence reactance of one line
X_M—Mutual zero-sequence reactance between lines
k—Relative distance from H to fault

occasionally trip three breakers. For the sake of simplicity assume that the zero-sequence impedance external to the two parallel circuits of figure 1*a* is the same at both ends and that a fault occurs at X in line A near H. If the line zero-sequence impedance is fairly high in comparison with that of the grounding transformers at the ends, a large proportion of the zero-sequence current will initially flow through the neutral of the grounding transformer at H and the relays of circuit breaker 1. In the meantime current will flow in a tripping direction and possibly of tripping magnitude through breakers 3 and 4, but presumably breaker 1 will open before either of these operate or no kind of selectivity would be obtained. After breaker 1 has opened, it might appear that the major part of the ground current would now return through the neutrals of the transformers at T since these are apparently nearer the fault than those at H. As a matter of fact, if the mutual zero-sequence impedance between lines is one-half or a greater proportion of the self-impedance of one line, then one-half or more of the ground current will return to the neutrals of the transformers at H. This is true regardless of the length of the circuits providing only that the mutual reactance is one-half or more of the self-reactance, and also assuming equal impedance of the grounding transformers. See figure 1*b*. As a result of the opening of breaker 1, breaker 3 relay then gets more current than breaker 2 relay, but by no means so much more as might have been expected, and accordingly, breaker 2 relay, if set without consideration of the effect of mutual impedance, may succeed in tripping before breaker 3 has cleared the short circuit.

Mutual zero-sequence reactance is no respector of persons and is not concerned with whether the two circuits involved have any electrical connection with each other or not. Its action is not confined to parallel circuits of the same voltage or even the same frequency and this has occasionally resulted in some rather mysterious behavior on the part of directional ground-current relays. Figure 2 illustrates a condition which has been known to cause operation of directional ground relays on one overhead grounded-neutral system that is paralleled by an electrically separate grounded-neutral system for a part of its length. Ground faults on the second line cause flux linkages with the first line that generate a voltage between the ends of each of the three conductors. Since both ends of every conductor of the first line are connected to ground through the wye windings of wye-delta transformers, there is a comparatively low impedance path around which these induced voltages can force a flow of current. It will be observed that at one of the transformer banks the direction of current flow is from the ground to the neutral and up through the winding and out over the transmission line while at the other transformer, the flow is reversed, being in on the line and down through the transformer to the ground. The relative direction of the current in the neutral and the current in the line is the same in both cases and is such as to cause the relays to act as if there is a flow of fault current into the line at both ends. If this is sufficient in size and duration, one or both of the directional ground relays will operate to clear a circuit which is electrically isolated by a double transformation from the faulty section.

An even more mysterious and obscure case occurred on an underground system in a city where there are 25- and 60-cycle cables in the same duct bank under the street as shown in figure 3. One of the 60-cycle cables formed a portion of a loop circuit which was protected with directional ground-current relays. On several occasions faults on the 25-cycle system that caused ground current to flow in one of the 25-cycle cables in the duct would trip 60-cycle directional ground relays. At first thought it might seem that the metallic sheaths of the cables would act to shield the conductors of one cable from the flux field around the conductors of another carrying ground current. Actually the sheaths do have some such effect, but on account of the relatively high-resistance material of which they are composed, they are not a very effective shield. Accordingly, there were enough effective flux linkages with the conductors of the 60-cycle cable to force sufficient current around the loop of which it was a part to operate the ground relays. If the system is grounded at the source only, the ground-relay torques are in the directions indicated in figure 3, and increase progressively away from the grounding point. If the system has more than one ground the current circulating in the earth would change the torques, but half of the relays in the loop tend to trip.

Where two parallel identical circuits have unequal mutual coupling with a third circuit, any ground current in the

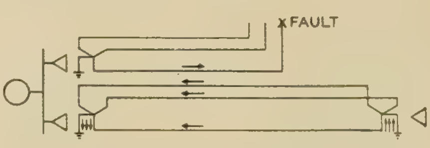

Figure 2. Flow of currents in a circuit exposed to zero-sequence induction

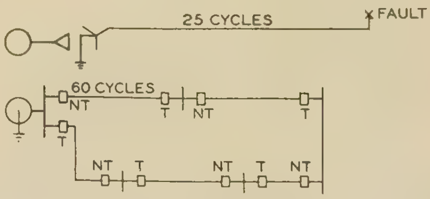

Figure 3. Flow of current in a loop exposed to zero-sequence induction

T—Tends to trip
NT—Tends not to trip

latter generates a voltage around the loop formed by the two parallel circuits. This voltage produces a circulating current which is superimposed on any ground-fault current that may be flowing in the two circuits at the moment, thus tending

to increase the current in one line and reduce it in the other. Where the lines are protected by balanced current relays in the residual circuit of the current transformers, faulty operation may result from the superposed circulating current on top of the balanced through current. The circulating current may be calculated by subtracting the mutually generated zero-sequence voltages in the exposed section and dividing it by the loop zero-sequence impedance of the two circuits.

Three or more parallel circuits on the same right-of-way necessarily have unequal mutual zero-sequence impedances so that through ground-fault current will not divide evenly between them. The middle circuit will carry less current than the others which may cause false operation of ground-current relays.

Mutual zero-sequence reactance is open to indictment on still another count. It is one of the factors that make the use of distance relays for ground protection so complicated that it becomes very nearly impracticable. If it were not for the mutual reactance between circuits, distance relaying for ground faults would be no more complicated than for phase faults, but the fact that the voltage generated between the fault and the relay location in the faulty conductor may contain a large mutual zero-sequence component makes it necessary to take into account in each relay the zero-sequence current in all parallel circuits in order to secure accurate distance measurement. It is quite apparent that where there are several circuits on the same right-of-way, the problem becomes pretty complicated if the currents in all of them have to be conducted to the relays in all the others. It is quite probable that this is the chief reason why distance ground relaying has not become very prevalent.

Although not strictly induction, there is another source of unbalanced residual currents in parallel lines that must be considered when very fast relays are used. When one circuit of a pair is carrying load and the second circuit breaker on the other is closed to parallel them, its poles will strike sequentially electrically regardless of the excellence of mechanical adjustment. There is a moment during which only one wire of the incoming line is in parallel, then a second interval when two wires are completed and finally all three are closed. For the first two intervals there is a zero-sequence current in each line. Where the system neutral is ungrounded this current flows only in the loop formed of the two lines and is equal in each and, therefore, would not tend to operate residual balanced current relays. Where there are grounded neutrals on both ends of the lines there is a true zero-sequence current flow via the grounding transformers and the earth and it is unbalanced in the two circuits.

For the condition of one pole of the breaker closed, the current may be calculated from an equivalent circuit in which the negative and zero-sequence impedances as viewed from the breaker are placed in series and inserted in the positive-sequence network as a series impedance at the breaker location. When two poles are closed the negative- and zero-sequence impedances are paralleled and connected in series with the positive-sequence network at the breaker location. From calculations like these the effect on balanced and directional ground relays may be determined.

Ground-fault currents generate voltage in any mutually coupled conductor without regard to the use to which it is put. Pilot wires used for relaying, telemetering, and similar purposes may have excessive voltages induced in them if they are in close proximity to aerial or underground power conductors carrying zero-sequence currents. The voltage is usually very small between wires but the voltage to ground may be high on all of them, thus endangering the insulation.

Mutual resistance, too, has caused insulation failures in pilot wires and equipment connected to them. The fault current returning from the earth to the neutral of a grounding transformer passes through whatever resistance there is between true earth and the station grounding system to which the transformer neutral is connected. The resultant IR drop displaces the station ground from true earth potential. The cases of equipment connected to the pilot wires are ordinarily grounded to the station ground and the pilot sheath is usually grounded purposely or accidentally to the earth along its length. The insulation of the equipment and the pilot wire in series is subjected to the potential between station ground and earth and is stressed in inverse ratio to the capacitances between the equipment and station ground and between the pilot wires and their sheath. In most cases the equipment capacitance is small and its insulation gets most of the voltage across it. Sometimes the reverse is true.

Having outlined some of the effects of mutually induced current, it is quite appropriate to discuss some of the methods which have or may be applied to circumvent the harmful consequences. The simplest point of attack is in the relay setting. In the first instance discussed it will often be found that once the correct distribution of residual or zero-sequence current has been determined, a change in the settings of the relays will give the desired selectivity.

In figure 2 the relay at the station where both circuits are grounded can be made to recognize the true direction of fault current by energizing its polarizing coil from the sum of the secondary currents of current transformers in all of the power transformer neutrals. The ratio of these current transformers should be in the inverse ratio of the voltage ratings of the system in whose neutral they are connected so that an equal kilovolt-amperes in each system will give equal secondary currents. The actual fault current which is doing the inducing will be greater than the induced current (in kilovolt-amperes) and hence the net current fed to the relay polarizing coil will be reversed from what it would have been had it been energized from a current transformer in the neutral of only the power transformer to which its line is connected. At the other end of the circuit this remedy is not available because the fault current itself is not present in this station. For this location there is no universally applicable remedy, but an expedient has been used that should be successful in many installations. The line-to-neutral voltages of the circuit in which the induced current flows are usually higher during the induced-current condition than they are while a ground fault exists in the line itself. This fact may be utilized through the use of three instantaneous undervoltage relay elements, the coils of which are energized from the line-to-neutral voltages and the contacts of which are all in parallel and the group in series with the directional relay whose misoperation is to be prevented.

While the author is not aware of any installation made for the special purpose of reducing mutual zero-sequence reactance between circuits, it is a fact that counterpoises and good-conducting ground wires tend to reduce the mutual zero-sequence reactance. This will be readily apparent when it is considered that the presence of these conductors brings into proximity with the inducing current, a returning ground current of opposite direction. This returning current also has a mutual coupling and since it is in the reverse direction tends to cancel out part of the voltage generated by the outgoing current in the parallel circuit conductor. If all of the ground current could be persuaded to return in a ground wire or counterpoise spaced the same distance from the circuit in which the voltage

is induced as the inducing circuit conductors, it would entirely cancel out the mutual reactance between the circuits. A perfect result is unobtainable but may be approached through the use of good conducting ground wires and counterpoises and by increasing the separation between the parallel circuits through the use of separate rather than twin circuit towers. It is not proposed by the author that this should be adopted as an economical remedy for false operation of ground relays, but is merely pointed out as another one of the advantages incident to the use of counterpoises and ground wires.

The use of conducting shields for pilot wires has been proposed since the cost is not prohibitive as it may be for power circuits. Where momentary interruptions are not harmful vacuum gaps or Thyrite resistors have been used to limit the voltage from pilot wires to ground. In other installations a higher insulation level has been provided, capable of withstanding the induced voltages. Occasionally neutralizing transformers[3] have been installed to allow station equipment to stay at station ground potential and the pilot conductors at true earth potential. Insulating transformers at the station boundary have also been employed where the quantity sent over the pilot wire is alternating current. The choice between the various remedies is one of economics for the particular installation.

Conclusion

Mutual impedance exists between adjacent circuits carrying zero-sequence or ground current. Its effect is to cause a different distribution of ground current than would have existed without it. Failing to take it into consideration has occasioned incorrect operation of ground relays, usually of the directional type. It has also caused false operation of balanced ground-current relays. Its existence makes distance ground-fault protection difficult. It also endangers the insulation of pilot wires and equipment connected to them. A number of remedial measures are available, but there is no universal, economical panacea. Each instance requires individual consideration to determine which of the several available remedies may best be utilized.

References

1. Symmetrical Components (a book), C. M. Wagner and R. D. Evans. McGraw-Hill Book Company.

2. Simultaneous Faults on Three-Phase Systems, Edith Clarke. AIEE Transactions, September 1931, page 936, appendix C.

3. Neutralizing Transformer to Protect Power Station Communication, E. E. George, R. K. Honaman, L. L. Lockrow, and E. L. Schwartz. Electrical Engineering (AIEE Transactions), May 1936.

Discussion

R. P. Crippen (Ebasco Services Incorporated, New York, N. Y.): In 1931 trouble was experienced from incorrect relay operation on the 13.2-kv system of the Tennessee Public Service Company in Knoxville, Tenn.

This system was fed from a single 110/13.2-kv substation of about 30,000 kva. Six overhead 13.2-kv tie lines ran from this substation to various other substations about the city. These substations were in turn joined together by other 13.2-kv tie lines. There resulted a network of 13.2-kv circuits, most of which were not more than two or three miles long and many of which were arranged two or three to a pole line. No transpositions were used. The system was grounded through a 20-ohm resistor in the neutral of one of the 10,000-kva transformer banks at the main substation.

Tests were made and it was found that for straight ground faults induced zero-phase-sequence currents up to a maximum of 90 amperes would flow in unfaulted lines. This indicated that ground relays on the lines thus affected should have a current setting which would allow for the induced currents.

A more difficult problem arose when simultaneous ground faults occurred at different points on the system on different phases, as sometimes happened. The resultant unbalanced currents in the lines were several times as great in magnitude as the current to a single ground fault, which was limited by the 20-ohm resistor. These heavy currents circulating between the two ground faults resulted in unbalanced currents in individual circuits which were much heavier than the straight zero-phase-sequence currents to ground faults. The resultant "mix-up" from unbalanced and induced currents was bewildering.

Reasonably good operation was obtained by increasing the current setting of certain of the ground relays, by applying the directional control feature to both phase and ground directional relays at certain points, and by installing faster-acting relays at two or three points.

The case illustrates what can occur under certain conditions and what is undoubtedly occurring to a lesser degree in many instances. Fortunately effects such as this are not usually sufficient in magnitude to cause any trouble to the relay engineer.

R. E. Neidig (Metropolitan Edison Company, Reading, Pa.): Mr. Bancker's paper will be of decided interest to every relay engineer who is faced with the problem of relaying grounded-neutral systems, and while the subject is not new, I believe that it has been given too little publicity in the past.

Several years ago it was discovered that mutual induction was the cause of incorrect relaying on one section of the 110-kv system of the Metropolitan Edison Company, and it is believed that a review of that instance at this time will be interesting.

Figure 1 of this discussion shows the central section of the aforementioned 110-kv system. The circuits between South Reading and Glendon are on two-circuit steel towers, and likewise are the circuits between Glendon and Gilbert. However, the latter tower line is on the same right-of-way with the South Reading-Glendon tower line for a distance of six miles out of Glendon. There are no ground wires on either tower line.

After experiencing a few cases of instability at Gilbert plant from 110-kv faults, high-speed balanced-phase and ground relays were applied at the three stations shown, and the oil circuit breakers modernized at Gilbert and Glendon. Immediately after this modernization program was completed, it was discovered that ground faults occurring on the South Reading-Glendon circuits were, in a number of cases, causing peculiar ground-relay operations on the Glendon-Gilbert circuits. In most cases where these peculiar operations occurred, the oil circuit breaker on one circuit at Gilbert was tripped by the balanced-ground relay, and the oil circuit breaker on the other line at Glendon was tripped by its balanced-ground relay. Occasionally only the oil circuit breaker at Gilbert was tripped. Obviously, something had to be done about it.

This type of operation indicated that the residual currents in the Glendon-Gilbert circuits was becoming unbalanced for some reason, and as this condition did not exist for faults beyond Gilbert toward West Wharton, it suggested induction in the six-mile section out of Glendon as the probable cause.

Inspection showed that the maximum induced voltage in the Glendon-Gilbert circuits would occur during a ground fault on the adjacent South Reading-Glendon circuit at a point where the two tower lines left the common right-of-way. Results of calculations made under these conditions are given in figure 1, showing that the loop current

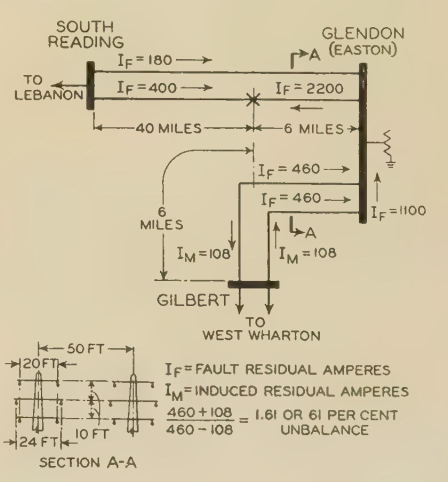

Figure 1. Section of 110-kv system of Metropolitan Edison Company affected by induced current and results of calculations for typical case

induced in the Glendon-Gilbert circuits caused as much as 61 per cent unbalance in their balanced-ground relays, which was far more than sufficient to cause operation.

There were two apparent means for

eliminating the peculiar operations caused by this condition, (1) reducing the sensitivity of the balanced ground relays, requiring either changes in the relay or additional auxiliary relays, or (2) removing the balanced-ground relays from service. While the latter means of correction may seem more or less rash, it must be pointed out that the current obtained in the faulted phase during a ground fault at any location on the Glendon-Gilbert circuits is very high, and of sufficient magnitude to operate the balanced-phase relays. Inspection of operating records showed that in practically no case did a balanced-phase relay fail to operate together with the balanced-ground relay for ground faults on these circuits. This fact, determined quickly and easily, led to the temporary disconnection of the balanced-ground relays, although thus far they have not been restored to service. Each circuit, however, is still equipped with back-up ground-relay protection.

It will be interesting to note that ground faults on the Glendon-Gilbert circuits produced no apparent peculiar operations of the South Reading-Glendon circuits, due, no doubt, to the appreciably higher loop zero-sequence impedance of these circuits. The balanced-ground relays on these circuits are still in operation, although it was necessary to reduce the sensitivity of them due to the difficulty encountered from sequential operation of oil-circuit-breaker poles, a source of trouble also referred to in Mr. Bancker's paper.

Wm. E. Marter (nonmember; Duquesne Light Company, Pittsburgh, Pa.): Mr. Bancker has presented a very interesting paper on the effect of induced current on relaying. This problem has been encountered in many places on the Duquesne Light Company system and in one instance has been successfully solved by a scheme installed during 1932, and which to my knowledge has not been published.

Figure 2 of this discussion shows the arrangement of 66-kv lines on which trouble was encountered. The lines were on double-circuit towers over separate right-of-ways with one Pine Creek line and one North line on each tower line. A fault near North substation on a North-Colfax line caused a circulation of induced current in the pair of lines between North and Pine Creek and between Pine Creek and Colfax.

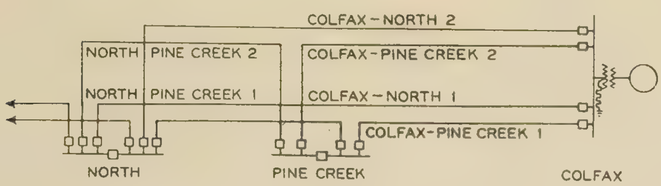

Figure 2

The protection used on the lines was seven-post balanced *CR* relay protection with both phase and ground relays. The circulating induced current in the lines added together in the neutral and caused incorrect operation of the ground relays, opening one breaker on the opposite end of each of the four lines in which induced current was circulating.

The protection used on the lines and the relay which was used to prevent incorrect operation is shown in figure 3. The fact that the induced current flowed in each of the three-phase leads to the balanced relays and this was the only condition which would produce three in-phase currents in the phase leads on a ground fault was used to determine the lockout condition. The current coils of three double directional elements were connected in series with the balanced phase *CR* relays and a zero-sequence potential from star-delta potential transformers was connected to the potential coils of each element.

On a ground fault, potential was produced due to the ground and if induced current existed all three directional elements operated in the same direction. The contacts on each side were connected in series and operated an auxiliary relay to prevent tripping by the ground relays.

On this system a 63-ohm neutral resistor is used so that the zero-sequence potential is always of about the same value and comparatively large. The ground current is limited in value and the ground relays have sensitive settings. The induced current was of sufficient magnitude to operate the ground relays but did not operate the phase relays and it was not necessary to provide a lockout for them.

This scheme was in successful operation for several years but has been removed due to line rearrangements on the towers which removed the induced current problem.

An additional installation was made about 1932 on the 22-kv system using the original shop model of the relay. Balanced protection was used on only one end of the pair of 22-kv lines and one relay could be used in this location to prevent induced-current operations.

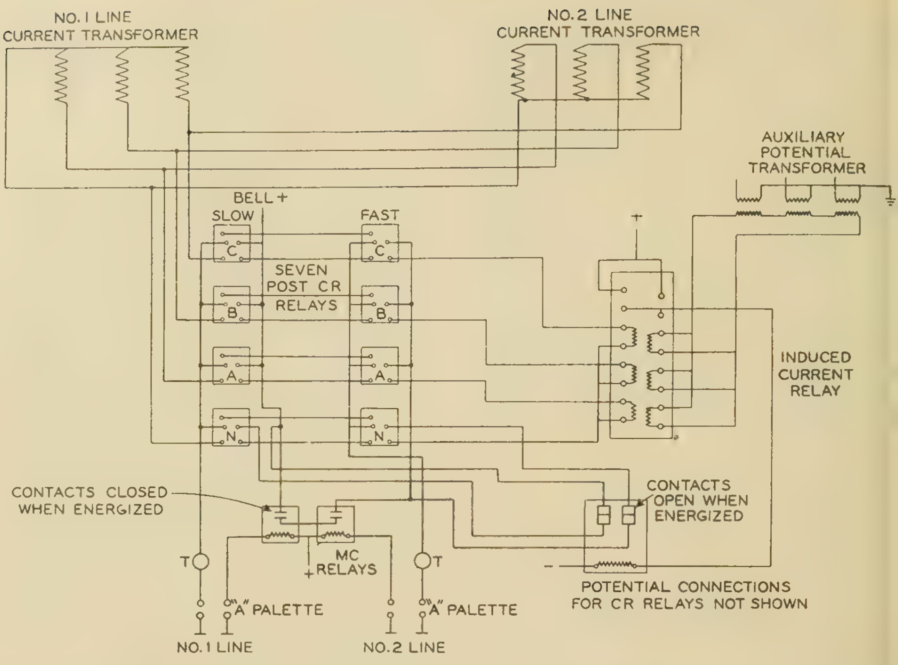

Figure 3. Induced-current relay used with seven-post CR balanced line protection

W. A. Lewis (Cornell University, Ithaca, N. Y.): Mr. Bancker has performed a service to electrical engineering by collecting in a compact form the references to the numerous problems produced by mutual induction between parallel circuits.

Many of the engineers working in this field have accepted these problems as they arose and solved them one by one, without realizing that the uninitiated would find them difficult when suddenly confronted with one of the more unusual problems. Hence, by describing in a simple fashion all the related problems, Mr. Bancker has provided a convenient and useful reference.

In describing the case of two parallel lines, after one breaker opens, Mr. Bancker shows that the greater portion of fault current may come from the end nearest the fault, even though the circuit breaker at that end has already opened, particularly when the mutual impedance is greater than half the self-impedance. The reason for this is, of course, that the mutual impedance reduces the effective impedance around the loop. Our experience has been that on double-circuit lines, where both circuits are on the same tower or pole, zero-sequence mutual impedance is often as great as seven-tenths of the self-impedance, so that the effect described is usually very important.

Mr. Bancker has explained that the use of distance relays for ground fault protection has been retarded by the mutual effects. As I was a party to the first paper which explained, more than seven years ago, how distance relays could be used for ground-fault protection on parallel lines, to give an accurate determination of the fault location, it is perhaps appropriate to discuss additional reasons why more widespread use of

distance relays for ground faults has not been made. The first of these is cost. With distance ground relays, three relays at each terminal location are required in order to protect against a fault on any phase. In some cases a smaller number of relays has been used, but some form of additional relay is necessary to provide switching so that the relay can be connected to the proper phase for the fault which occurs. This compares with a single ground relay in the schemes of ground protection now in common use. The second difficulty is the effect of fault resistance during a two-line-to-ground fault. Because of the phase relationship between the voltage drop in the fault resistance and the voltage drop in the line, it is found that even with a reactance relay a fault frequently may appear to be closer to the relay location than it actually is, thus sometimes producing an incorrect operation. To overcome this difficulty, it is necessary to arrange the relay so that it will not operate when the fault involves two phase conductors. This necessitates either considerable complication in the relay design or the use of some additional fault-selection device. However, I venture to predict that if single-pole switching of single-conductor faults comes into common use, distance relays will find an extensive application for ground protection.

Bert V. Hoard (Westinghouse Electric and Manufacturing Company, Newark, N. J.): Mr. Bancker has discussed a number of cases where mutual impedance between two or more lines or loops might cause false operation of directionally controlled ground relays. He also discussed certain methods of correction so that the relays would operate properly. Among these was the use of the summation current through all the transformer neutrals for polarizing the directional element, and the fact that if ground wires have been installed or the distance between the respective circuits is large, the effects of mutual induction are materially decreased.

There is available another relatively new method which, we believe, will become increasingly important as it becomes better known. This is the use of a negative-sequence directional element instead of the conventional zero-sequence directional element in a relay, which uses negative-sequence voltage and current for determining the direction of the fault from the relay. In a grounded system for any fault-to-ground there are always negative-sequence currents produced as well as zero-sequence currents. However, the negative-sequence currents must flow in the line conductors; hence their effect by mutual induction practically cancels in any adjacent circuit even though the spacing between circuits is small. It is only on those systems where the wires of the adjacent circuits are not transposed frequently enough and a fault occurs within a relatively long untransposed section that there might be appreciable unbalanced induction in one circuit due to negative-sequence currents flowing in the other.

This method of directional control is available in a new type *CRS* relay which consists of a conventional zero-sequence overcurrent element directionally controlled by a very sensitive negative-sequence directional element and its filters. It will be noted that only two potential transformers connected open delta are required for polarization, while for a conventional zero-sequence voltage-polarized relay three star-connected potential transformers are required; hence, considerable economic saving may be made for many installations when using the new relay.

For loop circuits an instantaneous negative-sequence fault detector may be required and can be obtained with the relay to produce selectivity between a fault in the loop and a fault external to the loop. As an example, in figure 3 of the paper assume the faulty circuit is 60 cycles and is connected to the 60-cycle bus, also that a generator is available and running at the remote bus. During a fault external to the loop this generator feeds power back through the loop, but the negative-sequence current flowing will be small when compared to that flowing when a fault occurs within the loop. The negative-sequence fault detector is adjustable so that it can be set to select between internal and external faults. Its contacts are in series with the directional element so that the zero-sequence overcurrent element cannot trip until the negative-sequence current exceeds the fault detector setting and is in the tripping direction for the directional element. If the above-mentioned generator is not operating, there should be negligible negative-sequence current flowing in the loop circuit for an external fault.

It will be seen that the big advantage of using negative-sequence currents for the directional element and fault detector is that there is a relatively large difference in the negative-sequence current flowing in the loop for an internal fault compared to a fault external to the loop; while if zero-sequence currents were to be used, the mutual induction between circuits would cause the relative currents to be more nearly equal in one branch of the loop for both internal and external faults and thus limit the possibilities of selectivity.

Edward W. Kimbark (Northwestern University, Evanston, Ill.): There seems to be a common impression that zero-sequence current is always associated with ground current. Mr. Bancker helps to correct this impression by giving two instances of circuits in which zero-sequence current is present without any ground current: (1) the loop circuit of figure 3, either ungrounded or grounded at one point, in which a zero-sequence current is induced from another system; (2) a similar loop circuit with a series open circuit of one or two conductors, due for example to the sequential closing of the poles of a circuit breaker. Nevertheless, Mr. Bancker lapses into the common error when he mentions "a true zero-sequence current" in case 2 if the circuit is grounded at two points. A third instance, one not mentioned in the paper, is a short circuit between a conductor of one circuit and a conductor of another circuit, as on a double-circuit line. (By "ground current" in the foregoing is really meant current in the neutral of a grounding transformer, for current will circulate in the earth beneath the loop of figure 3.)

I wish to discuss the circuit of figure 3 in which false operation of zero-sequence ground relays on the loop may be caused by induced currents from a ground fault on another system of the same or a different frequency. If the loop system is grounded through a high neutral impedance, it is especially susceptible to false operation of ground relays because the relays would be given low current settings. There are several respects in which conditions on the loop circuit during induction caused by a ground fault on another system differ from conditions during a ground fault on the loop itself, and some of these conditions might be used to discriminate between the two events. For example, during a line-to-ground fault on the loop itself, there is negative-sequence current and accompanying negative-sequence voltage. The negative-sequence current in the fault is equal to the zero-sequence current, though the distribution of negative-sequence current throughout the system may be somewhat different from that of the zero-sequence current. Negative-sequence relays are not susceptible to induced currents because the negative-sequence mutual impedance between the two systems is negligibly low. Negative-sequence quantities could be used to operate watt-type relays or to operate either the current element or the directional element or both of directional overcurrent relays. Relays working entirely on negative-sequence quantities would respond to all types of faults except three-phase, but this would do no harm. If set too low, they would respond also to unbalanced loads, and this fact, as well as the low value of negative-sequence voltage during line-to-ground faults, would prevent their use on a system grounded through high impedance.

On such a system another means of discrimination can be used. The zero-sequence voltage is high (practically equal to the normal positive-sequence voltage) during a line-to-ground fault, but is low during induced currents. Therefore a zero-sequence voltage relay could be used at each station of the loop to prevent operation of the zero-sequence ground relays unless the zero-sequence voltage should exceed a set value; or possibly the directional elements of the ground relays themselves could be adjusted so that they would not close their contacts except on fairly high torques.

J. A. Elzi (The Commonwealth and Southern Corporation, Jackson, Mich.): This paper calls attention to an important factor in the determination of the magnitude and distribution of zero-sequence currents where parallel lines are involved. The effects of mutual impedance are frequently quite surprising and make it very difficult to make a preliminary relaying layout until a complete study has been made. It is also very important that various system operating conditions and fault locations be investigated because a change in magnitude or direction of current in one line may greatly affect the currents in other lines.

Tests which were made recently on an extensive 22-kv grounded neutral system in which there are numerous parallel circuits in close proximity, demonstrated the above conditions very well. In this particular case, for example, a change of approximately two to one in current distribution between two lines occurred, due primarily to the differences in the effect of mutual impedance for two test conditions. The test values

were in quite close agreement with calculated values, which indicates that the methods of calculation now commonly used and referred to in this paper give very satisfactory results.

E. H. Bancker: As it was hoped, the discussions brought out several interesting cases of induced current experiences to supplement those cited in the paper. Professor Kimbark's and Mr. Hoard's discussion also points out another solution that may be adopted that was overlooked in preparing the paper. Negative-sequence induced currents should be relatively small in comparison with negative-sequence fault currents and, therefore, devices responsive to the negative-sequence should be free from tendency to misoperate for short circuits on other systems. As Mr. Hoard points out, their use sometimes results in a saving because only two potential transformers are required and these may be on the low-voltage side of a power transformer if it is certain that they will be energized at all times.

The main disadvantage in utilizing negative-sequence quantities lies in the fact that methods for deriving them are relatively inefficient in comparison with the methods of deriving zero-sequence quantities. In securing zero-sequence the sum of three current transformer secondaries or of three potential transformer secondaries is added together, giving three times the original quantity. To derive negative sequence it is necessary to use either a network or take the difference of two quantities containing positive-plus negative-sequence terms and positive-minus negative-sequence terms. In both cases the problem of deriving the true negative-sequence component is much more difficult than trying to separate out the zero-sequence component. In some of the instances investigated it was found that the negative-sequence components were too small a proportion of the total current or voltages to permit them to be utilized successfully.

A negative-sequence directional relay constructed from induction-cylinder elements known as the type *CBP* is available for use where the negative-sequence quantities are at least 15 per cent of the positive-sequence quantities. This relay consists of two elements operating upon a common shaft. One element is so connected as to produce a torque equal to the sum of the positive and negative sequence powers, while the other element produces a torque equal to the difference of these two powers. The resultant torque on the shaft is, therefore, either positive sequence or negative sequence, depending upon the connections used. These connections may be switched by a zero-sequence current relay so that the directional relay is normally responsive to positive-sequence power and operates in accordance with this quantity for three-phase and line-to-line faults. The presence of ground current switches the connections of one of the elements so that it becomes a negative-sequence power directional relay for all ground faults.

Professor Kimbark suggests that zero-sequence voltage might be used to distinguish between induced and actual fault current. There are probably cases when it would serve as, for example, in the system shown in figure 3. However, it is not universal in its application because the inducing current may be from the same system, in which case there would be as much zero-sequence voltage in the circulating loop as if the fault had been there. Also, if the system is like figure 2 the current circulating in the transformers produces as much zero-sequence voltage as the same amount of fault current, which conceivably might exist under some different generating condition. Accordingly, zero-sequence voltage is another possibility to add to the list, but is not an invariable selector.

It is hoped that this paper and its discussions will be of value to those who encounter the problem for the first time and wish to find out something about other cases of induced current and the solution adopted for preventing incorrect relay operation.

High-Speed-Relaying Experience and Practice

About 1930 the manufacturers introduced the high-speed circuit breaker with operating times in the order of eight cycles and at the same time engineers awoke to the possibilities of high-speed or one-cycle relays. The general requirements for such relays were set forth at the time in two papers[1,2] presented before the Institute.

After nearly ten years, a review of the experience gained with such relays seems to be in order, and to that end the relay subcommittee of the AIEE protective devices committee recently circulated a questionnaire among its membership designed to determine this.

This report is based upon the 13 replies received from typical operating companies and also the experience of the manufacturing companies in furnishing high-speed relays during the period. Because of the wide scope of the subject only the salient features can be touched upon here. For more detailed discussion, those interested are referred to the bibliography.

Instantaneous Overcurrent Protection

Most of the reporting companies use instantaneous overcurrent relaying, especially for ground protection, and report excellent performance. The reliability of the system as well as the improvement obtained from minimizing voltage dips and preventing burning down of conductors makes this method distinctly advantageous.

Paper number 39-15, prepared by the relay subcommittee of the AIEE committee on protective devices, recommended by the AIEE committee on protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 27, 1938; made available for preprinting December 6, 1938.

Paper prepared by J. H. Neher, chairman; C. A. Muller, L. F. Kennedy, G. W. Gerell, and R. M. Smith; all members of the AIEE relay subcommittee.

1. For all numbered references, see list at end of paper.

Settings reported for phase relays range from 100 to 200 per cent of the maximum symmetrical through fault current. While considerable benefit in clearing times can be gained in the case of long lines even though using a setting of 175 to 200 per cent, nevertheless one company reports over 1,000 installations using the 100-per-cent setting. An accurate record of the performance of instantaneous overcurrent relays has been kept by this company for two years which shows on steel-tower lines that 85 per cent of all faults are cleared by the instantaneous relays. On the portion of the system using mostly wood-pole line this record showed that 70 per cent of all faults were cleared by the instantaneous relays.

This same company sets the instantaneous ground relays at 110 per cent of the maximum through current and obtains correct operation.

One interesting experience noted was in the case of cable protection utilizing instantaneous relays. Here it seems that the fault is cleared so quickly that the cable will stand all tests and still break down later in service.

Distance-Relay Protection

Distance relays of the high-speed zone type have been used quite extensively for transmission-line protection against phase-to-phase faults. There are two kinds available: the reactance type whereby the distance is indicated from a measurement of the circuit reactance from the relay to the fault, and the impedance type which is based upon the measurement of the impedance.

The information obtained from seven companies who have distance-relay installations indicates that they have, in general, experienced entirely successful and satisfactory operation. Some unnecessary tripping has been occasioned

by out-of-step conditions which are, of course, liable to cause operation of any type of relay. Also, on some of the earlier type distance relays constructional defects have appeared and been eliminated.

Analyses of the effect of synchronizing surges and out-of-step conditions on the distance relay have been made so that trip-outs which were formerly classed as incorrect relay operations can now be shown to be the result of the complex conditions attendant upon out-of-synchronism situations.[3,4]

In addition to unnecessary tripping under out-of-step conditions, there have been trip-outs on wide angular swings in cases where the system would have pulled back into step if permitted to do so.

It should be borne in mind that the contacts of the directional and impedance units should be carefully adjusted to insure proper time co-ordination particularly to take care of restoration of normal conditions on a heavily loaded line. A few unnecessary trippings have occurred because of incorrect co-ordination adjustments.

The distance relay is not generally applicable to protection against single-phase-to-ground faults unless elaborate methods of compensation are employed,[5] and as a result, very few installations of this type of protection have been made. One company, however, reported a number of installations of uncompensated impedance relays used for ground protection of important lines. On the whole, these have been successful when carefully applied on steel-tower lines with ground wires, but not otherwise.

No doubt in this case the successful operation was occasioned principally by the presence of the ground wire and lack of multiple neutral grounds. The ground wire has the effect of bringing the zero-sequence and positive-sequence impedance values closer together, thus mitigating the necessity for compensation.

Pilot-Wire Protection

The use of pilot-wire protection for relatively short lines has been constantly increasing and general use has been made of both a-c and d-c systems. The a-c systems are of the differential variety comparing currents at the line terminals usually with a relay having a percentage characteristic. The d-c systems, like the carrier-current protective systems, are of the directional comparison types, but use has been made of both the blocking type paralleling the carrier-current protective equipment, and the tripping type.[6,7] The tripping type appears to be most popular largely because it uses simpler relay equipment, although its application is somewhat more limited than is the case with the blocking type. All of these systems use relays operating in 0.01 second or less with a very marked tendency to the greater use of one- or two-cycle relays. Operating experience with these systems has been very good. Most of the d-c systems employ telephone-type pilot wires and the reports indicate that these circuits are giving very reliable service.

A recent development in a-c pilot-wire protection involves the use of extremely sensitive pilot-wire relays fed from phase-sequence networks which permits the use of a single telephone pair as the pilot wire to obtain protection against all types of faults.

Carrier-Current Pilot Protection

Carrier-current relaying has not as yet been adopted by many operating companies. However, such experience as those companies using it have obtained, indicates that distinct operating advantages are possible. The equipment itself being comparatively new might be expected to disclose numerous difficulties. Operating records of one company with many installations indicate that 1.2 per cent of the operations were incorrect, about one-third of which were chargeable to the carrier equipment.[8] The majority of the faults in the carrier equipment were due to a certain physical arrangement of equipment which failed to provide adequate weatherproofing. The majority of the faults in the relays were due to imperfections in relay or circuit design which have since been corrected.

Balanced Protection of Parallel Lines

Modern high-speed balance relays have a very definite application to many power systems,[9] and many installations have been made of both the current-balance and the cross-connected directional relay types. Such protection, of course, is only effective for single-line faults at a time when both of the paired circuits are in operation, and other relays must be installed to provide protection during single-line operation or in the case of double-line faults.

The application of balanced protection must be considered carefully since there are a number of factors tending to cause incorrect operations. Experience has shown that attention should be given to matching reasonably the current transformer characteristics on the two or more lines supplying the relay. If this is not done a through fault may readily cause operation of the balance relay, such phenomena having been reported on at least one system.[10]

Considerable difficulty has been experienced in applying this type of protection to lines which terminate on different bus sections interconnected through a tie breaker which may be tripped automatically and thus destroy the balance of the lines.

Protection of Lines With More Than Two Terminals

Lines having more than two terminals usually present relaying difficulties. There are available no schemes or methods of relaying especially designed for this application. The high-speed schemes available for two-ended lines such as distance, instantaneous overcurrent carrier, and pilot wire are used on tapped lines, but the high-speed protection rarely extends to all posts of the tapped line. The high-speed coverage with distance and overcurrent to be expected depends on the relative line lengths from the tap point of the terminals and will vary, if the equivalent impedance beyond each terminal changes.

Carrier and pilot systems using a directional indication for the basis of discrimination may also present difficulties because power may initially flow out of the section at one terminal for an internal fault, thus blocking the tripping of all terminals. This trouble can usually be cured by the use of high-speed distance or overcurrent backup relays.

Nine companies reported that they had no tapped or branched lines on high-voltage systems, hence had no application of high-speed relaying for tapped or branched lines. The schemes submitted by the other four companies were all based on the fact that the magnitude of the short-circuit current for faults in the section where it was not desirable to relay the line was not sufficient to operate the high-speed relays. In cases where it was impossible to accomplish this by the above means, either pilot-wire or carrier-current relaying had to be used.

Bus Protection

High-speed relays of either the overcurrent or impedance types have been applied in various bus protective systems and experience seems to indicate that satisfactory operation is being obtained. This is discussed in a report on bus protection currently presented before the

Institute (AIEE TRANSACTIONS, volume 58, 1939, pages 206–11).

The application of the complete differential system with instantaneous overcurrent relays is becoming more common, although it is recognized that there are conditions which may result in a sizeable difference current flowing in the differential circuit during the first few cycles of a fault. From the available data it appears relatively safe to use instantaneous overcurrent relays on the bus differential system if the current transformers are chosen high enough in ratio so that they are never subjected to more than ten times their rated current under any condition.

A new development during the past year is the harmonic-restraint type of relay which offers a method of obtaining fast clearing of internal faults and at the same time preventing incorrect operation on the initial transient in the case of through faults.[11]

Effect of Transients on High-Speed Relays

The fact that transients are encountered which may affect relay operation has been reported from several sources. The effect of these transients on high-speed relays may either cause incorrect or unnecessary operations or operate to prevent operation of the relay when desired until the transient has disappeared.

Many of these transients have been present without causing any incorrect operation with the older types of time-delay relays. They become of more importance with the high-speed devices capable of operating in a total time of one or two cycles.

The most commonly encountered forms of transients and the corrective measures available at the present time are given in table I. A few general comments in relation to this table may be in order. The effect of asymmetrical current waves on overcurrent or distance relays is, of course, obvious. Operating experience, however, indicates that so few short circuits produce sufficient offset to cause incorrect operation that the fact may in general be disregarded except in the case of equipment installed on generating station busses.

Transformer magnetizing current is a well-known phenomenon which all transformer differential relays for years have attempted to take care of either by time delay or desensitizing equipment. The new factors introduced within the past year are the use of a tripping suppressor attachment or a harmonic restrained relay which recognizes the difference in wave form which occurs during the period of magnetization.

Table I. Types of Transients Affecting the Performance of High-Speed Relays

Type of Transient	Type of Protection	Result	Corrective Measures
Asymmetrical current wave	Instantaneous overcurrent or distance relays	Over reaching. Incorrect tripping	Use of transient shunt
Transformer magnetizing inrush	Transformer differential relays	Incorrect tripping	Use of a desensitizing arrangement, tripping suppressor, or harmonic restraint
Expulsion-gap operation	Instantaneous overcurrent or distance relays	Incorrect or unnecessary tripping	Add time delay relay in tripping circuit
Nonsimultaneous breaker-pole closure	Ground relays	Incorrect tripping	None
Current-transformer saturation	Differential protection, particularly for busses	Incorrect tripping on external faults	Change in current transformers or use of current or harmonic restraint
Potential-device secondary degree transient	Distance relays	Slow tripping	None

The use of expulsion gaps obviously subjects relays to short-circuit conditions for a short period of time, and if the relaying is to be fast under other conditions unnecessary tripping will result. It seems more desirable to use the existing high-speed devices for this service and enable them to ride over expulsion-gap operation by adding a definite-time auxiliary relay in the trip circuit.

In general nonsimultaneous breaker-pole closure has not been a source of trouble, although two or three cases have been reported where a transient maintained itself in the residual circuit long enough to cause unnecessary relay operations. The methods of correction have varied with each particular case, being largely dictated by the types of relays already installed. No general solution is available.

Potential devices in many cases will have a transient between the occurrence of a fault and the time the secondary voltage reaches its new level. This will prevent operation of distance relays until the voltage becomes low enough for the relay to operate. There is no solution to this available at the present time.

Conclusions

While it is apparent that the "ideal relay scheme" so earnestly sought after[12] has not yet been found, nevertheless it may be said that the efforts made in that direction have not been in vain, and that the following conclusions are evident.

1. The instantaneous overcurrent relay provides a simple inexpensive means of obtaining high-speed relay protection over a considerable portion of the line length and offers possibilities in the way of general relay-system improvement of which, to date advantage has not been fully taken.

2. Distance relays have proved themselves reliable if carefully applied and maintained, and are widely used. However, the relative complexity of this device, its general restriction to phase-to-phase fault protection only, and the fact that instantaneous protection is not provided for the entire line length, have caused engineers to look elsewhere for the ideal relay protection.

3. Carrier-current or metallic pilot protection appears to be the first choice of high-speed protective systems whenever the expense is justified. Experience has indicated that a remarkable degree of reliability can be expected from the communication channel whether it be of the carrier-current or metallic type.

4. High-speed bus protection can be successfully applied if proper steps are taken to prevent the operation of the differential relays on transients.

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Discussion

S. L. Goldsborough (Westinghouse Electric and Manufacturing Company, Newark, N. J.): As is pointed out by the authors, some unnecessary and, at the time, unexplainable trip-outs of high-speed distance relays have been caused by out-of-step conditions. This should be no reflection on the distance-type relay, since it is obvious that it, as well as any other type of relay, should view the out-of-step conditions as representing an actual fault. Nothing effective was done to prevent the operation of distance relays on these conditions, other than the analysis of the situation, until the advent of the high-speed carrier systems. The availability of a carrier signal seemed to render the problem of supplying out-of-step blocking easy of solution. However, at least in the case of the carrier system employing a three-zone impedance relay, it turned out that the carrier signal played a very minor role in the operation of the out-of-step blocking scheme developed. In other words, the identical out-of-step blocking system used with the stepped-type impedance-relay carrier system can be used to provide out-of-step blocking for conventional impedance relays without carrier. The only function of the carrier signal in the out-of-step blocking scheme is to enable the relays to trip on an internal three-phase fault after the system is out of synchronism. Therefore, when this out-of-step blocking system is used on impedance relays without carrier, the ability to trip for three-phase faults during out-of-step conditions is secured only when the backup time element is arranged not to be blocked by the out-of-step relays.

Concerning the tendency of distance relays to trip on wide angular swings which would not result in out-of-step conditions, analysis has brought out that the probability of tripping on wide angular swings is minimized by using the same currents to actuate both the impedance element and the directional elements. For instance, if delta current is supplied to the impedance element then delta current should be supplied to the directional element and if star current is used it should be supplied to both elements. This precaution will not prevent all trippings on wide angular swings but the tendency to trip will be materially reduced.

While the 13 typical operating companies contacted are doubtless a fairly representative cross section of the field, it is felt that the paper would have been more valuable, both in the amount of information obtained and the number of conclusions deducible therefrom, if a large number of operating companies had been contacted.

V. E. Verrall (General Electric Company, Philadelphia, Pa.): This summary of high-speed-relaying practice comes opportunely after a period of intense development at a time when applications of high-speed relays will be made with increasing frequency. During this development we have met a succession of difficulties with transient electrical conditions, some in the relay windings and some in the protective circuit, conditions which were not important with the induction-disk relays because the transients were all over by the end of two to three cycles. In Europe the difficulties were avoided by introducing time delay of the order of six cycles before the relay is allowed to complete the tripping circuit. In this country we have devised means for overcoming each transient trouble. For example, modern distance-relay dephasing circuits are made electrically deadbeat; directional-relay potential circuits are provided with time constants which will give correct action. As time went on these snags have become fewer and fewer and now the one-cycle relay seems to be thoroughly practical.

Nevertheless, it may be wise to avoid the maximum relay speed when such speed will not add materially to the performance of the protective system. In general one-cycle relays can be adjusted to give a time delay of two or three cycles, thus achieving a desirable margin of safety. In a distance relay, for example, an extra cycle or two in the response of the directional element avoids the need for the refined co-ordination of directional and impedance contacts mentioned. The ill effect of nonsimultaneous breaker-pole closure upon ground relays will usually be avoided by using induction relays of the high-speed type with a few cycles time delay under these conditions instead of relays working in less than one cycle.

While the operation of carrier equipment has been successfully controlled during out-of-step conditions, nothing appears to have been done in this country to control distance relays. It is a simple matter, however, to prevent undesirable tripping of distance relays by a low-set fault detector which opens the trip circuit if the line impedance changes slowly indicating a power swing as opposed to the instantaneous change in impedance when the line is short-circuited.

About 80 General Electric reactance relays are now being used for ground faults. Satisfactory operation has been reported over the last three years. Reactance relays are much preferable to impedance relays on ground faults because there can be very appreciable resistance in the fault contact and ground circuit which is liable to vary between wet and dry periods.

In table I of the paper the use of the transient shunt is suggested for preventing undesirable operation on asymmetrical current waves. A simpler solution is the use of induction-type relays, such as the high-speed induction cup, which are substantially unresponsive to the d-c component of the current wave.

Paul O. Langguth (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): While the committee states that no solution to the problem of potential-device transients is available, our experience indicates that satisfactory relay operation may be obtained despite these transients. We have used these applications under field short-circuit conditions. The tests on the Indianapolis system[1,2] show that the high-speed relays energized from potential devices gave the high-speed clearance of faults for which they were installed. There were no false or delayed operations. These results would seem to indicate that with proper co-ordination of potential device, auxiliary transformer, and relay characteristics, satisfactory operation of high-speed relays from potential devices is entirely reasonable and to be expected.

Although transient disturbances may be expected in a loaded potential device (due to the combined network of capacitance and inductance) still the combined circuit of burden and potential device constitutes essentially a series circuit of R, L, and C. In such a circuit it is well known that as R is increased relative to L and C the transient becomes of shorter duration and the phenomena changes from oscillatory to deadbeat as R passes the critical value $R^2 = 4L/C$. The R in this case represents essentially the burden resistance. Thus it can be seen that at light burdens the resistance becomes high and the transient is nonoscillatory. Even with full rated burden on the devices the magnitude of the damped wave may be reduced to the point where it will have negligible effect on the operation of high-speed relays.

In view of the satisfactory performance obtained on the Indianapolis and other systems there must be some differences in the application which are not immediately obvious. Therefore, it would seem desirable to analyze further those systems giving slow operation in order to discover the specific causes of unsatisfactory operations.

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J. R. North (The Commonwealth and Southern Corporation, Jackson, Mich.): This report of the relay subcommittee is very interesting and decidedly encouraging as regards the use of high-speed relaying. However, referring to the second paragraph under "Distance Relay Protection," it might be inferred that distance relays have, in general, given entirely successful and satisfactory operation except for some unnecessary tripping occasioned by out-of-step conditions. Conclusion number 2 mentions the relative complexities of distance relays and the necessity for their careful application and maintenance.

The experience of one of our companies with an installation of some 30 high-speed zone-type reactance relays may be of interest.

These relays were installed in 1931 and, from the beginning, numerous incorrect and unexplainable (at the time) operations occurred. These operations were studied and discussed with the manufacturers. Several steps were taken to improve the performance of the relays and also the performance of the 161-kv bushing potential devices which supplied the relays with potential.

The potential burden of the relay starting unit was much higher at low voltage than at rated voltage and this accentuated the

ratio and phase-angle errors of the bushing potential devices. The performance of the starting unit was improved by bringing three-phase potential to the relay and removing the tuned resistance-capacitor unit, which caused the high burden at low voltage. This change decreased the relay pickup from 12 to 6 amperes and it was therefore necessary to add a resistance to compensate for this.

The bushing potential devices were given elaborate phase-angle and ratio tests in the field, and it was found that their variations in ratio and phase angle not only gave incorrect ohmic indication, but that this ohmic indication varied for maximum and minimum fault conditions. The ratio error tended to reduce the relay pickup current, thus, making the relay more apt to operate under low-current conditions.

The bushing-potential-device networks were subsequently rebuilt to give more efficient operation but, even after these changes and with the relays having constant burden, there were still a number of incorrect operations. During the following year there were 8 correct and 3 incorrect operations on phase faults and 15 incorrect operations on ground faults. In this classification, which is based on careful analyses of oscillograms, faults which involved two or more phases, with or without ground, are termed "phase faults." The term "ground faults" applies only to single line-to-ground faults.

These relays were provided for phase fault protection only and were not intended to operate on ground faults; however, the oscillograms indicated that the secondary phase currents during ground-fault conditions on this extensive isolated-neutral system might be as high as 20 amperes. The phase angle varied widely but appeared to be generally in the range of 45 to 75 degrees, leading the power-factor position.

After further detailed study and discussions with the manufacturers, it was the consensus of opinion that possible causes of the trouble included:

(*a*). Improper angle of maximum torque of starting unit.

(*b*). Excessive wipe of ohm-unit *A* contact.

(*c*). Combination of load and fault currents giving incorrect distance measurements.

(*d*). Errors in potential devices and current transformers.

(*e*). Proximity effect of adjacent relays.

It was decided to make further changes in the relays to eliminate the difficulties. The auxiliary resistance in series with the potential-coil circuit, which had been installed to bring the pickup back to its original value, was replaced with a reactance unit in order to shift the angle of maximum torque from unity power factor to 45 degrees lag. This change in the angle of maximum torque tended to prevent incorrect operation on ground faults and to improve operation on two-phase and three-phase faults.

Auxiliary wye-delta-connected current transformers were installed further to reduce the operations on residual ground current.

The adjustment of the relay mechanism was carefully checked and the contacts readjusted where necessary.

The changes outlined herein have resulted in very much improved operation of these relays according to the records to date, and it is felt that the detailed analyses of the conditions in the field, including the tests on the bushing potential devices and relays, were decidedly worth while. No doubt this work has also assisted materially, although indirectly, in the development of the improved present-day designs of distance relays.

In view of this experience, it is our opinion that proposed applications of high-speed zone-type relays should be carefully investigated, weighing the over-all gains against the complexities and possible operating difficulties. The performance of the high-speed unit and the characteristics of the potential and current supply require careful co-ordination.

E. L. Harder (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This report, having its basis in the actual operating experiences of numerous companies, forms a most valuable guide to protection engineers.

The effect of potential-device transient on distance-relay speed would bear further analysis. For faults over a considerable portion of the operating range of an impedance element the voltage does not need to fall nearly to its new level before a large operating force is available. In view of the extremely short time constant of decay of the error voltage the time extension is scarcely measurable in this region. Near the relay balance point the relay time increases even when tested with an abrupt voltage change. Due to the increased relay time in this region, the short-duration transient becomes of small importance. It would seem rather difficult to separate time extension due to proximity to the balance point from that due to potential-device transient. It would be interesting to know whether this phenomena has been observed and measured or whether it has been anticipated on theoretical grounds.

Under the heading of bus protection the guide given for freedom from current-transformer transients, namely current not to exceed ten times transformer rating, would need to be used cautiously.

It is apparent that the assumption of average design and ten times current leaves the rather important variable factors of d-c time constant and relay burden unaccounted for. The time constant may vary over the range 0.3 to 0.003 or possibly more in different instances, longer time constants of 0.1 to 0.15 seconds being typical of large generator busses. Burden, while varying less from an average value, is likewise a very important factor, particularly the resistance component.

Methods are now available for calculating without too great complication what differential current will occur. Where long time constants or high burdens are encountered the rough guide should be used with caution and it is considered preferable to calculate the differential current.

L. F. Kennedy (General Electric Company, Schenectady, N. Y.): Mr. North has presented the case history of a distance-relay application which did not operate successfully until certain changes were made as the result of field tests. The undesirable operations were due mainly to three causes, namely: (*a*) conditions existing on an extensive high-voltage, isolated-neutral system when one conductor was grounded, (*b*) variation in ratio of potential devices, and (*c*) improper relay-contact adjustment. The operations resulting from the conditions existing with one phase grounded were correct as far as the relays themselves were concerned, but actually they were the consequence of what was later recognized as an incorrect application of the relays as originally designed. In other words, the relay had not been correctly designed for this particular application. Often complete data are lacking in cases of early application of new devices so that changes such as outlined by Mr. North become necessary as greater experience is gained. This experience indicates the necessity for full knowledge by all concerned of system, relay, and associated equipment characteristics if correct operations are to be obtained.

In regard to the general use of potential devices, these are giving satisfactory operation when properly applied. To obtain the best results proper consideration must be given to the ratio, particularly under limiting fault conditions. There are existing data showing a transient in the voltage circuit that tends to delay operation slightly but as indicated by Mr. Harder, this is not generally noticeable with present over-all clearing times.

Mr. Langguth has mentioned that this transient may be controlled to some extent. It is not generally necessary to have such a refinement today, but it would be highly desirable to have this information more generally available.

Some of the discussers have indicated that it may be desirable to slow down the speed of relay response in order to eliminate some of the possibilities of incorrect operation. Through the co-operative efforts of manufacturers and users, we are now at the point where we have overcome most tendencies of high-speed relays to operate incorrectly. It is our feeling that the work done to date has reached the point where practically the same degree of reliable operation can be obtained with high-speed relays as with lower-speed devices.

With the availability of breakers operating in less than eight cycles any concerted move to slow down relay operation seems to be a backward move and it is our recommendation that the present policy of making high-speed relays thoroughly reliable even with one-cycle operating times be continued.

An Amplifier-Wattmeter Combination for the Accurate Measurement of Watts and Vars

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Synopsis: As part of a program of improvements made to the network analyzer in the electrical-engineering research laboratory at the Massachusetts Institute of Technology, an instrument to measure watts and vars has been devised which imposes a negligible burden upon the network, is rapid in response, and has an error less than one-half per cent of full scale. The instrument consists of (1) a semistock electrodynamic wattmeter, (2) a negative-feedback vacuum-tube amplifier, and (3) a phase-shifting network. When the equipment is once assembled the presence of the amplifier may be ignored and the instrument used thereafter as any portable instrument. The principle of instruments of this kind has other important applications in the field of electrical measurements.

Purpose of the Instrument

THE development of the instrument described herein forms part of a program of improvements intended to overcome the limitations of certain measuring instruments which were originally provided for use with the MIT network analyzer[1] when it was built jointly by the department of electrical engineering at MIT and the General Electric Company during 1928–29. Certain instruments which possessed the characteristics necessary for this application, and which could be constructed at that time with reasonable cost, had various undesirable limitations, the chief one being slow response.

The nature of the characteristics required of certain instruments used with a network analyzer can best be explained by describing briefly its function. Such a device permits electrical representation in miniature of an actual power system. The MIT network analyzer operates at 60 cycles, and is equipped with sufficient elements to represent the electrical essentials of a power system comprised of as many as 16 generating stations with interconnecting lines and loads. In most studies, whether they be load-distribution, steady-state, or transient-stability measurements of volts, amperes, watts, and vars at numerous points within the network are to be performed. Since an average load study may often necessitate a thousand or more readings during a day's work, the need for measuring instruments that are rapid in response without a sacrifice in accuracy is apparent. The accuracy requirement imposes unusual limitations upon the measuring instruments, and unfortunately it is not always satisfied merely by the use of accurate measuring instruments. In addition, the burden represented by the instruments must have negligible effect upon the quantities they are to measure.

When it is necessary to perform measurements at the terminals of the equivalent synchronous machines, the commercially available portable instruments of short response time can be used, since it is not difficult to correct for whatever effects their losses introduce. For convenience, these instruments usually remain connected at the machine terminals throughout a study and are ignored while measurements are made at points within the network.

Unfortunately, when commercially available portable instruments are connected at points within the network, their burden often appreciably disturbs the electrical conditions previously established. The labor then necessary to readjust the circuits to compensate for these disturbances, or the tedious calculations involved if the data are corrected analytically for them, in part defeats the purpose of a network analyzer. To render these disturbances negligible the impedance of the current coil of an instrument connected in the network and the admittance of the potential circuit of an instrument connected at the point of measurement in the network, must be much smaller than are normally encountered in commercially-available portable instruments having the desired sensitivity, accuracy, and speed of response.

When the MIT analyzer was built the quantities generally measured were volts, amperes, and watts. The low-burden voltmeters and ammeters then provided were thermocouple instruments and the low-burden wattmeters had suspended moving elements. All these instruments were slow in response on account of their necessarily low burden. After the analyzer had been used for several years, developments in the art of power-system analysis placed more emphasis upon the knowledge of vars than had been the case originally. To measure vars, a phase-shifting transformer was used to make available at the terminals of a wattmeter a voltage in quadrature with the voltage at the point of measurement. To speed the measuring process, a commercially available low-power-factor portable wattmeter having low current-circuit impedance was used as the indicating instrument for watts and vars, and its potential-circuit burden supplied from the phase-shifting transformer. This procedure relieved the network of wattmeter potential-circuit burden and permitted the use of a rapid-response instrument. However, it still necessitated: first, to measure watts, the manual adjustment of the secondary voltage of the phase-shifting transformer to equal in magnitude and angle the voltage at the measuring point; and second, to measure vars, the rotation of this voltage into quadrature with the voltage at the measuring point. Thus the time required to make these measurements was considerable.

The instruments originally used to measure voltage within the network have been replaced by a single a-c rectifier-type voltmeter of 1,000 ohms per volt. The burden of this voltmeter is negligible, it reaches full deflection in a fraction of a second, and is provided with an adjustable shunt across its moving coil to permit rapid standardization by comparison with a standard portable electrodynamic voltmeter. The instrument described hereinafter has been developed to replace the apparatus originally used to measure watts and vars, with the object of reduc-

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Paper number 39-8, recommended by the AIEE committee on instruments and measurements, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted June 30, 1938; made available for preprinting November 29, 1938.

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The authors acknowledge the assistance given them with this problem by many of their colleagues at the Massachusetts Institute of Technology. They also wish to thank R. N. Slinger of the General Electric Company for his helpful comments made during the preliminary discussions on this problem, P. J. Jacobs, Jr., now of the Allis-Chalmers Manufacturing Company, who carried out the early experimental work, and A. H. Wolferz of the Weston Electrical Instrument Corporation for his co-operation in obtaining a suitable wattmeter.

1. For all numbered references, see list at end of paper.

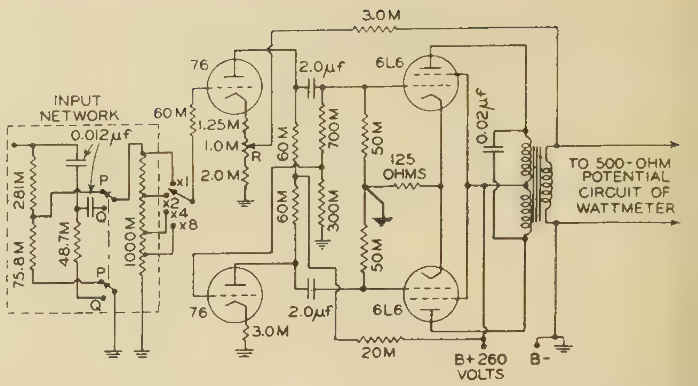
ing the time required to make these measurements. In a manner discussed toward the end of the paper, this instrument is also used for the rapid measurement of current in polar or complex form.

Form of the Instrument

Since it was expected that only one instrument of this type would be needed for the particular application, it appeared desirable to use stock apparatus wherever possible in order to avoid excessive costs. In accordance with this idea it seemed logical to consider continuing the use of an electrodynamic wattmeter for the basic indicating member of the instrument. Then, to reduce the time to take measurements it was proposed to operate the wattmeter in conjunction with a vacuum-tube device that would supply all, or part of, the wattmeter energy for operation, and, at the same time, eliminate the phase-shifting transformer. Hence, a study was made to determine whether vacuum-tube units would be required for both potential and current circuits, and if both were not required, which would be preferable.

If a single vacuum-tube amplifier is used to energize the wattmeter current coil, its input circuit could consist of a low-resistance shunt connected in series with the network. The burden imposed by the wattmeter potential circuit must then be made negligible by decreasing its admittance and hence decreasing the ampere turns provided by the usual moving-coil system if assembled from stock wattmeter parts. Since the rapidity of motion of the moving-coil system in an electrodynamic instrument depends on the product of the ampere turns of the moving coil times the ampere turns of the stationary coil, a moving system of low burden and short response time necessitates more than normal ampere turns for the fixed coil, in this case the current coil. It was found that to provide sufficient ampere turns by suitable stock coils would overload the current coil to a degree that would make its temperature rise excessive. On the other hand, if a single vacuum-tube amplifier is used to energize the wattmeter potential coil instead of the current coil, its design is less difficult. The admittance of its input circuit can be made small enough to impose a negligible burden on the network, and the impedance of the wattmeter current coil can be made small enough to comply with the limitations imposed by the analyzer application, and both restrictions thus satisfied with one amplifier. A 90-degree phase-shift circuit can be inserted in the amplifier input network to make possible the measurement of vars. However, when the impedance of the current coil of a wattmeter of conventional design is reduced, the number of turns in the coil is usually reduced. Thus, the potential coil must supply more than normal ampere turns to give the ampere-turn product required by a rapid and rugged moving-coil system. Fortunately, the potential circuit in most designs of stock wattmeter operates with a larger margin of safety in regard to heating than does the current circuit, and it is possible to establish an ampere-turn product in the potential coil sufficient to provide a satisfactory instrument without serious heating.

Figure 1. Circuit diagram of amplifier



For these reasons, the instrument consists of a single amplifier which provides the potential-coil current of a semistock electrodynamic wattmeter whose current-coil impedance is small enough to have negligible effect upon the electrical conditions of the network.

Design of the Wattmeter

Because there appears less freedom in the design of a wattmeter from stock parts than in the design of an amplifier, the wattmeter was selected first and the necessary amplifier then designed. The specifications established for the wattmeter limit its current-coil impedance and specify its sensitivity. The ampere-turn product then required, and hence the ampere-turns to be provided by the potential coil, necessitates a compromise between the allowable time for the pointer to come to rest and the degree of ruggedness desired in the instrument.

The criterion which limits the magnitude of the current-coil impedance is the magnitude of the smallest impedance element used in the network analyzer. More specifically, if inserting a current coil into a network is not to disturb appreciably the electrical conditions of the network, the current-coil impedance Z_i must be negligible compared with the impedance Z_L measured looking into the network from the point where it is opened up to insert the coil. The criterion established was that the disturbance would be considered negligible provided the value of the impedance Z_i was less than one per cent of the impedance Z_L. The impedance values encountered in a study are a function of the base quantities and with the analyzer connected for the customary 200-volt two-ampere base, unit impedance* is 100 ohms. Operating experience has indicated that impedance elements less than 0.10 per unit on the system base are seldom encountered, although in an extreme case two circuits each represented by about a 0.10 per-unit impedance might be connected in parallel. For this condition it can be shown that the minimum value of the impedance Z_L ranges around 10 ohms; therefore the impedance of a current coil must be less than 0.10 ohm.

The current coil of a 5- or $7^1/_2$-ampere wattmeter for low power factors has an impedance close to this value and was therefore selected. When used with the analyzer the working coil current is determined by the magnitude of the quantities to be measured rather than by temperature considerations. Since unit power is 400 watts or a convenient multiple thereof, and, since it is desired to indicate with reasonable accuracy about a 0.01 per-unit load or 4 watts, an instrument having a sensitivity of about 75 watts full scale, 100 volts, unity power factor was considered reasonable.

A moving-coil system was next desired that would have a restoring torque

* Power-system computations are usually made in terms of per-unit quantities, where unit or base kilovolt-amperes is an arbitrarily chosen magnitude, unit voltage is normal voltage, unit current is equal to $\frac{\text{base volt-amperes}}{\sqrt{3}\ \text{volts line-to-line}}$, and unit impedance is equal to $\frac{(\text{volts line-to-line})^2}{\text{volt-amperes}}$. To study a system on the MIT analyzer, a single-phase representation is used and convenient unit quantities are 400 volt-amperes, 200 volts, two amperes, and 100 ohms. Thus, on this analyzer 400 volt-amperes represents unit system kilovolt-amperes, 200 volts represents unit system voltage, and two amperes represents unit system current. Other analyzers use different base quantities.[6]

sufficient to provide a reasonably fast and rugged instrument, and yet not so large as to require an abnormal number of potential-coil ampere turns to provide the necessary ampere-turn product with about 0.75 ampere in the current coil. The moving system of a Weston model 310 portable electrodynamic wattmeter for low power factors was made suitable for this purpose by (1) use of restoring springs that have approximately 70 per cent of normal torque, (2) operation with approximately twice the customary potential-coil current, and (3) slight modification of the air-damping system. The wattmeter as assembled gives full-scale deflection at unity power factor with 0.60 ampere in the current coils, and is calibrated zero to 60 watts. A series-parallel current-coil connection and potential multipliers provided as described hereinafter, permit appreciable increase in range. For convenience in calibration a 100/200-volt potential circuit is provided. Table I summarizes the principal design constants of the wattmeter. Although the above modifications to a stock portable wattmeter may make it somewhat less rugged, they are not considered serious because it is removed from the central metering desk only when taken to a standardizing laboratory.

Design of the Amplifier

If an amplifier is to be suitable for use as a component of a measuring instrument for a network analyzer, it should satisfy the following specifications:

(*a*). A constant ratio of input voltage to output current throughout its working range.

(*b*). A high degree of stability during long periods of time.

(*c*). A high degree of freedom in regard to the interchangeability of tubes of the same type.

(*d*). A negligible phase angle between input voltage and output current.

A type of amplifier whose performance approaches the above specifications is one whose circuit employs the principles of negative feedback,[2-5] and is accordingly used in this instance. A circuit diagram is given in figure 1. The form in which the negative feedback is employed in figure 1 results from the operating conditions encountered in this application. To avoid supplying an excessive amount of energy from the amplifier, it is desirable to energize the wattmeter potential circuit at as low a voltage as practicable. However, it is not satisfactory to energize only the potential coil, unless the change in its resistance due to temperature rise has negligible effect on the desired ratio of coil current to amplifier input voltage. Since temperature rise may often be appreciable (see table I) the amplifier could be designed using inverse feedback in a manner that tends to maintain the ratio desired to a high degree in spite of the effects of temperature rise in the potential coil or other elements which form part of the output circuit. To do this a resistor having negligible temperature coefficient would be connected in series with the potential coil, and a percentage of the voltage drop across this resistor fed back into the input circuit of the amplifier, so as to oppose the applied input voltage.

To determine the quantity of feedback voltage required, it is necessary to investigate the dependence of the amplifier performance on the expected variation in output-circuit resistance resulting from temperature rise. The elements which probably contribute most to this effect are the potential coil and the output-transformer windings. Using the customary methods of analysis[2,3], the voltage drop E_2 developed across the feedback resistor in the output circuit, is related to the voltage E_1 applied to the amplifier by the expression

$$E_2 = \frac{\mu E_1 - I_c Z}{1 + \mu\beta} = I_c R_0 \qquad (1)$$

where

- μ is the ratio of the voltage appearing at the primary of an equivalent 1:1 output transformer, to the grid-cathode voltage of the first tube
- β is the fraction of the voltage E_2 fed back into the input circuit
- I_c is the potential-coil current
- Z is the sum of the impedance Z_T of the output transformer and Z_c of the potential coil
- R_0 is the feedback resistor of negligible temperature coefficient connected in the output circuit

By rearranging equation 1 the transconductance g_m, which is equal to I_c/E_1 becomes

$$g_m = \frac{\mu}{R_0(1 + \mu\beta) + Z} \qquad (2)$$

From which

$$\frac{dg_m}{dZ} = \frac{-\mu}{[R_0(1 + \mu\beta) + Z]^2} \qquad (3)$$

From equation 3, the absolute fractional change $\Delta g_m/g_m$ in g_m caused by a change ΔZ in Z, when all other parameters are constant, is,

$$\frac{\Delta g_m}{g_m} = \frac{\Delta Z}{R_0(1 + \mu\beta) + Z} \qquad (4)^*$$

Table I

Potential circuit	
Potential-coil current (ampere)	0.08
Ampere turns	24
Resistance of moving coil (ohms)	45
Temperature rise of moving coil (deg C)	12.7
Inductance of moving coil (henry)	0.0041
Torque per spring at 100 degrees (milli-gram-centimeters)	70
Number of springs	2
Current circuit	
Working current series connection (ampere)	0.6
Maximum current series connection (amperes)	5
Number of current coils	2
Turns per coil	34
Resistance per coil (ohm)	0.034
Ampere turns (with usual working current)	40.8
Movable system	
Undamped period (seconds)	1.02
Damping factor	18.3
Time to get reading at two-thirds scale (seconds; approximate)	2.5
Torque/weight[1.5] (milligram-centimeter for 100 deg/grams[1.5])	33.4

Equation 4 permits calculation of the magnitude of the resistor R_0 necessary to maintain any desired amount of constraint on g_m for the predicted variation ΔZ resulting from temperature rise, when appropriate values are assigned to μ and β.

In any actual circuit the parameters appear as complex quantities, but for this particular amplifier a real value is required for g_m because the potential-coil current must be in phase with the voltage applied to the amplifier. Measurements performed on an amplifier assembled so as to make $\Delta g_m/g_m$ essentially independent of anticipated changes in Z due to temperature rise, showed that the phase angle in g_m was not negligible. A phase-shift network could have been inserted in the feedback circuit to make the phase angle negligible, but it was not inserted because the phase angle becomes negligible if the drop across the circuit consisting of the resistor R_0 and the potential coil Z_c in series is used as the feedback voltage E_2. This connection depresses the effect of temperature rise in the coil only to the extent that it is masked out by the magnitude of R_0 then required, but it allows β to be a real quantity and simplifies making the small adjustments to the amplifier gain required when a standby wattmeter must be used. This latter feedback connection is the one adopted, and the temperature rise of the coil is made negligible by using an automatic-release on-off reversing switch to substitute the coil and the resistor R_0 for a dummy load of equal resistance only during the short interval required to read the instrument. This switching arrange-

* Similar expressions for variations with respect to μ can be derived.[3]

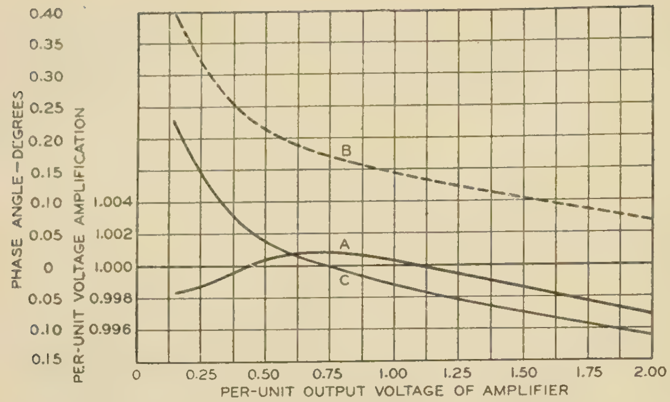


Figure 2. Characteristics of amplifier

Curve *A*—Per-unit voltage amplification

Curve *B*—Phase angle between input and output voltages

Curve *C*—Phase angle between input voltage and current in wattmeter potential coil

ment is also necessary to protect the coil against accidental burnout in the hands of operators of the network analyzer who might unknowingly overload it considerably for long periods.

The magnitude of the resistor R_0 necessary to mask the errors due to temperature rise in the coil to 0.5 per cent if operated continuously at normal working current, is about 450 ohms. This magnitude is used for the feedback resistor since it is not excessively large compared with that required to render negligible the effects of temperature rise in the remainder of the output circuit, and does not impose excessive burden on the amplifier. The wattmeter potential-circuit multiplier is used for the resistor R_0, by being tapped at a point which makes the coil resistance plus R_0 equal to 500 ohms. The amplifier output terminals are connected to this circuit, which represent a normal burden of approximately three watts at 40 volts.

The voltage amplification without the feedback and for the impedances shown in figure 1 is approximately 40. The negative feedback was increased deliberately to within a safe margin of the limit of stable operation of the system. This condition results in a net voltage amplification of about 2, and constrains the amplifier so that variations in its performance caused by changes occurring in tube characteristics are reduced to about five per cent of what they would be were no inverse feedback employed. For this over-all amplification, the voltage applied to the input of the amplifier is approximately 20 with normal current in the potential coil of the wattmeter. The cathode-bias resistors have no shunting capacitors because, if used, they introduce undesirable phase shift. The primary of the output transformer is tuned to reduce phase-shift caused by its exciting current.

The essential characteristics of the amplifier are indicated by the curves of figure 2. As seen from these curves the voltage amplification is essentially constant, and the phase angle between input voltage and potential-coil current is negligible throughout the operating range. Tubes of the same type having normal characteristics may be interchanged with a negligible change in performance. The instrument is ready to use within about one minute after applying voltage to its plate and filament circuits. The change in performance for a plus or minus four per cent change in voltage supplying the plate and filament circuits is negligible.

Input Networks

It is desirable to have the same wattmeter calibration and multiplying factors with the potential circuit supplied by the amplifier as with it supplied directly from the network. To give the desired multiplying factors for both watts and vars, two voltage-divider input networks are provided ahead of the amplifier, and each is adjusted so that approximately 20 volts is applied to the input of the amplifier when 100 volts is applied to the input terminals of either network. When the grid of the amplifier tube is connected to one input network, the current in the potential coil of the wattmeter is in phase with the voltage at the measuring point and allows the measurement of watts. When the grid of the amplifier tube is connected to the other input network, the current in the potential coil of the wattmeter leads the voltage at the measuring point by 90 degrees and allows the measurement of vars.

These input networks must impose a negligible burden upon the power-system network under study. To satisfy this condition, the criterion established was that their burden would be considered negligible if their magnitude was less than the magnitude of the uncertainty of the wattmeter reading. Thus, with a meter giving full-scale deflection for 60 watts and accurate to within one-fourth per cent of full scale, the burden imposed by the input networks of the amplifier should be less than 0.15 watt at 100 volts. The input network used to measure watts consists of wire-wound resistors, and imposes a burden of approximately 0.03 watt at 100 volts. The input network used to measure vars consists of resistances and capacitances and imposes a burden of 0.045 volt-amperes at 100 volts. The characteristics of this network are frequency sensitive; but, for the values of the circuit parameters, a change of one per cent in frequency changes the magnitude of the voltage applied to the amplifier by about one per cent and the phase-angle by about 0.2 degree. Since the frequency of the source varies less than ±0.2 per cent, the effect of frequency variations upon the characteristics of the phase-shift network is negligible.

The instrument is made to indicate either watts or vars by connecting the amplifier to the proper input network through a conveniently mounted switch. To extend the range of the instrument the grid of the amplifier tube is connected by another switch to different taps on the voltage-divider circuit as shown in figure 1. These provisions give the instrument a range from zero to 960 watts or vars.

In certain studies it has been found convenient to use one scale multiplier throughout a study, and to make the meter indication correspond directly to a convenient multiple of system base kilovolt-amperes by appropriate selection of analyzer base quantities. For example, if effective use of the analyzer equipment results when unit system kilovolt-amperes is 100,000, and unit analyzer volt-amperes is 400, the instrument reads system megavolt-amperes directly when the scale multiplier is four. Other convenient scale multipliers may be used.*

Calibration of the Instrument

Calibration of the amplifier-wattmeter combination instrument is accomplished readily. The wattmeter is first calibrated in the standardizing laboratory in the usual manner without regard to its ultimate use as part of the combination instrument. Then, when it is used as part of this instrument and its potential-coil current is supplied by the amplifier, the accuracy of the combination instrument is determined by comparing its indication

*H. P. St. Clair of the American Gas and Electric Service Corporation was the first to emphasize the value of this feature and as a result of his suggestions, the wattmeter dial has been graduated for zero to 24 as well as zero to 60 full scale to extend the applicability of this feature.

of a definite measurement with the indication of the same measurement given directly by the wattmeter when the appropriate tap on the potential circuit of the wattmeter is connected directly to the point of measurement. The voltage at the point of measurement must have the same value when each indication is read. The amplifier is made to have the desired amplification by adjustment of the tap connection on the resistance R of figure 1, which varies the feedback coefficient β. Because of the ease with which a check on the accuracy of the instrument can be performed it is done daily; but no readjustment of the feedback circuit has been found necessary during a ten-month period of use.

Application to the Measurement of Current

The instrument is useful to measure current rapidly in complex or polar form. To measure the in-phase and quadrature components of current the potential circuit of the amplifier is connected to an alternating-voltage source, preferably of 100 volts, whose phase angle may be regarded as the reference angle of the network under study. The current coil of the wattmeter is connected into a branch of the network. The instrument then indicates 100 times the in-phase and quadrature components of the current in this branch when the switch on the amplifier is connected to the "watts" and "vars" positions respectively. If the voltage is supplied by a phase-shifting transformer the angle of this voltage can readily be made any desired value.

To measure current in polar form, the switch on the amplifier is first connected to indicate vars. The phase-shifting transformer is then connected to the input circuit of the amplifier and the phase angle of its voltage adjusted until the wattmeter reads zero. The short time required for the pointer of the instrument to come to rest makes the operation a fairly rapid one. The voltage of the phase-shifting transformer is now in phase with the current in the current coil of the wattmeter. With the phase-shifter terminal voltage maintained at 100 volts, the instrument indicates 100 times the current when the switch on the amplifier is transferred to the "watts" position. The phase angle indicated by the phase-shifting transformer is the phase angle of this current with respect to the reference voltage of the phase-shifting transformer. The magnitude of the voltage is chosen as 100 while using this instrument to measure currents only to simplify the division of watts by volts to obtain amperes.

Conclusions

By employing a negative-feedback amplifier it has been possible to obtain an instrument using essentially stock parts which has the speed of indication of commercially-available portable instruments, and, at the same time, sufficiently low current-coil impedance and potential-circuit admittance that its burden is negligible compared with the quantities to be measured on the network analyzer. The instrument is accurate to better than one-half per cent of full scale for any range when indicating watts or vars. It is not unlikely that a device of this type has other important applications in the field of electrical measurements.

This instrument with associated equipment so expedites the metering procedure on the MIT network analyzer that the speed at which data can be reliably recorded controls primarily the time required for such a study. In addition, this equipment makes it practicable to include numerous refinements in the procedure employed when performing certain studies.

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Discussion

R. N. Slinger (General Electric Company, Schenectady, N. Y.): The authors of this paper are to be congratulated for their excellent work in making use of the negative-feedback amplifier circuit with a 60-cycle semistock portable wattmeter. While their primary objective was evidently to improve the speed and accuracy of their network analyzer instrument system they have perhaps also pointed the way to further fields of usefulness for precision type amplifiers of this type.

As they have indicated, the requirements for a wattmeter-amplifier combination suitable for application to a network analyzer are quite severe. Accuracy, speed of response, and extremely low instrument burden are all exceedingly important. The instrument combination also must be capable of retaining its calibration within close limits over relatively long periods of time and the indicating instrument must be of such form that it can be read easily and rapidly many times a day without undue eyestrain. In fact, almost the only common instrument requirement not included is the frequency-response characteristic, which is not of importance in this instance only because the analyzer reactor and capacitor circuit elements make a constant-frequency power source one of the essential components of a network analyzer.

Another important requirement not specifically mentioned in the paper is the ability to withstand frequent and severe momentary overloads. The range of currents that must be measured in the course of an average system study may easily be as great as 500 to 1. With magnitudes of the current and to a lesser extent the voltages, differing so widely, even the best of operators will occasionally inadvertently connect the instrument into circuits carrying several times the current or voltage they had expected, especially when the instrument is of a type specifically designed for high-speed operation.

It is not clear how the instrument described in this paper will be protected from serious overloads, particularly on low-power-factor readings, or how it will be arranged to cover a sufficiently wide range of currents. By proper design of the output characteristics of amplifiers for either current or potential circuits, they can be made to protect the instrument by limiting the maximum output to values that will not result in serious damage. It would be of interest to know how the authors have solved these problems.

They have indicated the convenient method in which the instrument lends itself to the measurement of components of current. It would seem equally important to be able to measure components of voltage as well. To do so, would necessitate connecting the current coil of the wattmeter to an adjustable power source to provide a reference current. The potential coil of the wattmeter could then be connected to the network at any desired point for the measurement of voltage components.

In the design of the input network, a slightly different arrangement of the constants might result in a better instrument working range. Most of the voltages in an analyzer representation of a power network will ordinarily fall within the range of 90 to 110 per cent of base voltage. Therefore, it might be better to design the input network to give a full-scale wattmeter deflection with 0.6 ampere in the current coil and 150 per cent of base voltage on the terminals of the input network. For load study readings this would ordinarily permit the use of only one potential-coil multiplier for all readings without danger of overloading the potential-coil circuit. The other multipliers for use in other types of studies could then be convenient multiples or fractions of this multiplier.

The authors' statement in regard to the interchangeability of tubes is quite in line

with experience obtained on the General Electric Company's network analyzer in Schenectady. This analyzer, using a similar circuit for the instrument amplifiers, as described in a paper presented at the winter convention a year ago, and using a light-beam type wattmeter constructed from conventional parts has now been in almost constant operation for a year and a half. The instruments have been periodically checked for calibration and some of the tubes in the amplifiers have been replaced as they showed signs of diminishing emission. However, no difficulty whatever has been experienced in operating the amplifier with some new tubes and some old ones and very little adjustment of any kind has been required since the instrument system was put into operation.

Eric A. Walker (Tufts College, Medford, Mass.): In the measurement of small amounts of power there is one source of error introduced by the wattmeter itself, which can be neglected only if certain precautions are taken to make this error as small as is possible. This error is one resulting from what might be called the transformer effect of the coils. The flux set up by the current coil of the meter generates a voltage in the voltage coil in addition to that impressed by the external circuit. This additional voltage causes an additional current in the voltage coil.

The same action takes place in the opposite direction: the current in the voltage coil causes a flux which gives rise to a current in the current coil. Both of these effects can be checked by energizing either the voltage or current coil in the proper manner and short-circuiting the other coil.

These two effects cause errors which are not constant but which are effected by a number of factors. These are: (1) the voltage impressed on the voltage coil; (2) the current flowing in the current coil; (3) the amount of coupling between the windings which changes with the position of the coils; (4) the impedance of the circuit connected to the voltage coil; (5) the impedance connected to the current coil.

As was said before, the error is not important in wattmeters designed for large amounts of power especially when the power factor is near unity. However, for wattmeters having a full-scale deflection of about five watts at power factors of 0.01 and below, the errors so introduced may be larger than the actual reading.

The difficulty may be overcome by arranging the circuits so that a high impedance is connected in series with both windings and the currents then caused by the transformer action are small.

W. O. Osbon (nonmember) and **W. W. Parker** (both of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors are to be congratulated for the thorough manner in which they have described the application and design of the amplifier-wattmeter combination for use with the MIT network analyzer. The paper contains data which will be interesting and useful to those concerned with both network calculators and negative-feedback amplifiers and their application.

The special interest in this paper results from the recent development of a negative-feedback amplifier but along somewhat different lines. This amplifier unit and associated instruments are shown in figure 1 of this discussion. Current feedback instead of voltage feedback is used which avoids difficulties and circuit complications due to heating of the instrument coils.

Without sacrificing quality of performance the amplifier assembly has been made very compact, being only 9 by 10 by 12 inches. It includes a power pack and two separate two-tube amplifiers, one for the voltmeter and the wattmeter voltage coil and the other for the ammeter and wattmeter current coil.

Due to the use of both current and voltage amplification it has been possible to use standard instruments as far as torque, speed of response, and damping is concerned.

The necessity for a large number of instrument readings in calculator studies makes it desirable to have direct readings to cover the range of values required for any one problem. This is done by using instruments which have two overlapping uniform scales. Direct readings are obtained of current, volts, watts, and vars without the necessity of multipliers for problems using the normal calculator voltage level. When the lower voltage level is used (primarily for short-circuit studies) the infrequent wattmeter readings are multiplied by 0.2, all other readings being direct. Phase angle and component quantities of currents and voltages are obtained on the "universal vectometer" which is used in conjunction with the amplified instruments. The "universal vectometer" has been previously described in the *Electric Journal* for May 1933.

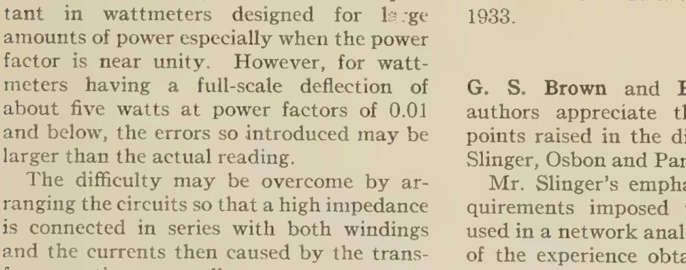

Figure 1

G. S. Brown and **E. F. Cahoon:** The authors appreciate the many interesting points raised in the discussions by Messrs. Slinger, Osbon and Parker, and Walker.

Mr. Slinger's emphasis of the severe requirements imposed upon an instrument used in a network analyzer, and his mention of the experience obtained on the General Electric Company's analyzer in regard to the freedom from tube troubles, are much appreciated. In answer to Mr. Slinger's question concerning the protection of the instrument against serious overloads, we would like to mention again that the current coils of the wattmeter are essentially those used in a stock meter rated for five amperes. As stated in the paper, this relatively large rating results from the low-current-coil-impedance criterion imposed by the analyzer application. Since the parallel connection of the coils permits working up to ten amperes, and since no generator on the MIT analyzer is rated more than six amperes, the danger of burnout of the current coils is not great.

As for the potential circuit, a small overload-alarm device is connected in parallel with the dummy amplifier load, this device and the dummy load being connected to the amplifier except when a reading is being made. The alarm device was not described in the paper in order to save space. It consists briefly of a relay energized through a diode rectifier from the output of the amplifier. The relay operates a warning light whenever the output voltage is (1) large enough to endanger the wattmeter potential coil, or (2) more than the amplifier can supply without departure from the linearity or phase-angle requirements. The warning light is located adjacent to the key that is operated to substitute the potential-coil circuit for the dummy load, and hence is readily noticeable.

Again referring to Mr. Slinger's discussion, the potential coil taps on the input network are used principally to change the range of the instrument, and seldom to match with some particular voltage base. If our design had permitted a simple means to change the current sensitivity the suggestion of an input network adjusted for about 150 per cent normal voltage as maximum voltage, might have become a practical matter.

The point raised by Professor Walker is an extremely important one. It is rarely mentioned in discussions of instruments to measure watts or vars, perhaps because the total potential circuit and total current circuit impedances are relatively large in the conventional instrument applications. To demonstrate the importance of this point, the deflection of the pointer in the wattmeter of the instrument described is about one-tenth full scale when the *potential coil* is short-circuited and rated current is applied to the current coil. However, with the 450-ohm feedback resistor connected in series with the coil and the series combination short-circuited no deflection can be detected. Similarly, the pointer deflection is again about one-tenth full scale with normal potential-coil current applied and the *current coil* short-circuited, but with 0.5-ohm impedance in series with the current coil and the combination short-circuited, no deflection can be detected. Since the equivalent current-circuit impedance is usually in excess of 10 ohms, as discussed in the paper, the instrument is believed to be free from the transformer effect of the coils. However, the authors are glad that this point was raised, because they are aware of other attempts to build an instrument of the type described that were unsatisfactory because of the relatively large currents induced by the transformer effect.

Notes on Emergency Ratings

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COSTS of money, depreciation, and taxes now represent almost two-thirds of all electricity-supply costs. Other costs have been reduced to the point where it is difficult to effect further large reductions in them. It appears, therefore, that the major opportunities for improving the competitive position of electricity must now lie in the direction of lowering the fixed costs by reducing to a practical minimum the investment required to deliver adequate voltage and the nearest practicable approach to continuous power supply.

Increased load ratings may represent one logical step in the solution of this problem. Such a step requires primarily the reconsideration of emergency load conditions, since these alone have almost traditionally determined when additions to load-carrying plant should be made. There are so many phases and so many different types of apparatus to be considered in any general program for increased load ratings that it would be quite impossible, if it were the intent of this paper, to go beyond a simple discussion of a few pertinent relationships and to outline some thoughts that may be helpful to others also working on this problem.

Almost more frequently than for any other single element in the distribution system, it becomes necessary to determine the extent to which cable of some sort determines the load rating which may be applied. In their relation to the system plan, most of the load-rating problems are so nearly parallel to those for cables that a careful discussion from the point of view of the cable plant not only permits drawing upon a wealth of convenient illustrations but at the same time should be helpful to the engineer who faces a similar problem for other apparatus. Let us consider for a moment the place of emergency ratings in the system plan.

Paper number 39-68, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 1, 1938; made available for preprinting December 29, 1939.

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The author is indebted to many writers on temperature, particularly to V. Karapetoff,[1] V. M. Montsinger,[2] Wallace B. Kirke,[3] Elwood A. Church,[4,5] and William H. McAdams,[6] whose works were most helpful. He is especially grateful to his associates for their contribution to such of his observations as may prove to have value.

1. For all numbered references, see list at end of paper.

Place of Emergency Ratings in the System Plan

Economy in fixed charges can be attained in each of two directions. The first is to provide economically the capacity needed to carry the transient load occasioned from time to time in the normal course of events when a sub-station supply cable, transformer, or some other link in the supply system, fails in service. This sort of emergency is almost a routine affair, to the extent that it is usual practice to select a design which will meet such a situation with the minimum practical need to disturb the load being supplied under the conditions. The gradual extension of the network principle to points nearer the customer's service take-off simply extends the limits within which such failures of equipment may occur without causing a customer's service interruption. The value of the emergency rating in this case lies in permitting each link in the supply system to carry as high an average day load as is possible, without in any way reducing the ability of the system to meet such transient emergency loads. The duration of such emergencies may range from, say, 12 hours to locate and repair a cable fault, or 24 hours to replace a substation transformer bank, up to perhaps three months to repair a large transformer and return it to service if a spare unit is not available.

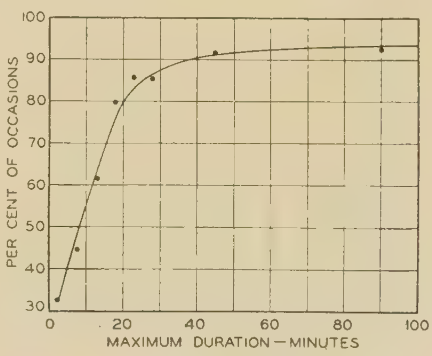


Figure 1. Per cent of occasions when alternate source is needed, versus their maximum duration

The second type of emergency includes the group of situations which accompany a major system disturbance. Under such conditions, the system operators may need to use every available supply link to the practical limit of its capacity, until it is possible to isolate the fault and return the load to its normal source of supply. Figure 1 summarizes for one system about 100 observations of the time during which it has been necessary to supply distribution substations from an alternative source, over emergency tie lines that are provided for such use while the normal source of supply is being restored to regular operation after a major disturbance. It will be seen from this that about 85 per cent of such cases require use of the alternative source for less than one-half hour. Hence, there may be occasional need for very high short-time emergency ratings. These may have little value for increasing the average daily loading of the normal supply elements of the system but will serve to reduce the extent of customer inconvenience to a minimum during a major system disturbance. It may even be helpful to have a one-hour and a two-hour as well as a one-half hour rating, so as to provide the utmost flexibility for redistribution of load, before it should be necessary to reduce loads to the lower permissible values for all-day emergency operation. One plan that has some attractive possibilities in operation of a supply cable system would permit following any one of the following emergency load schedules, in per cent of normal load:

(*a*). Two hundred per cent for one-half hour, followed by 130 per cent for the rest of the load cycle;

(*b*). One hundred sixty-five per cent for one hour, followed by 130 per cent for the rest of the load cycle;

(*c*). One hundred fifty per cent for two hours, followed by 130 per cent for the rest of the load cycle; or,

(*d*). One hundred thirty per cent for one complete load cycle.

Thus, the schedule to be followed in each given instance would be determined automatically by the magnitude of the initial load. Radial distribution feeders ordinarily may not require sufficient use of one-half hour or one-hour ratings to justify provision for them. However, the 150 per cent–130 per cent load schedule has the merit of providing time for load transfers in the field when this becomes necessary for the best redistribution of the emergency loads between the remaining circuits in an area affected by a service failure of one. This does not by any means exhaust the possibilities that will face the engineer in each of the wide variety of possible occasions for the use of emergency ratings. It does serve to

illustrate the occasional needs for meeting both short and comparatively long-time emergency load situations.

Perhaps the most important principle to be kept in mind in the consideration of emergency ratings is that the maximum emergency loads to be carried should not affect the ability of the system to operate satisfactorily under such extreme conditions, or be sufficiently severe, with due regard to their probable magnitudes and frequency of occurrence, to cause an unreasonable reduction in the average life expectancy of the facilities required to carry such loads.

Economy alone may often urge even more severe treatment of the cables and other load-carrying equipment than would be acceptable if its attainment should require measurable sacrifice in safety, dependability, or flexibility. The underground substation supply cable in a metropolitan system, for instance, may represent no more than 30 per cent of the total installed cost of line switching sections, useful duct space, manholes, and cables. It may be good business, therefore, to take advantage of an appreciable increase in average load per cable at the expense of a possible corresponding increase in cable maintenance costs. In many respects the situation is quite the same for substation equipment, etc. Decision as to the practical limit of this economy naturally requires consideration of all the factors in the light of experience and seasoned judgment. In appropriate circumstances, however, an above-average failure rate may represent more careful engineering than would a low failure rate.

The Magnitude of Emergency Loads

One of the facts that appears to be most confusing to those charged with direct responsibility for cable or apparatus ratings in electricity distribution is the usually very low average ratio of peak load to installed capacity. Surveys often show, for instance, that the average load per cable at the time of maximum station output is less than 70 per cent of its all-day emergency rating. Two factors, service continuity and load growth, almost entirely explain this difference between installed capacity and the average yearly peak load per cable. Nearly all additions to distribution plant investment are made to provide for the supply of load growth and to maintain, or improve, service continuity. Perhaps it would be helpful to illustrate the effect of these two factors by discussing their effect upon substation supply cable loading.

Let us assume that the load on a substation in one year is just equal to the normal load capacity of two supply cables. Also assume, tentatively, that the emergency and normal load ratings are identical for these cables, so that a third cable has been provided in order to have two cables available, should one fail. In the following year, however, a small increment of load growth requires the installation of another cable and, in that year, four cables are needed to supply but little more than a two-cable load. Load growth will then gradually absorb the spare capacity, until a time is reached when the load on the substation will very nearly equal the capacity of three cables. During the interval between the installation of the fourth cable and the time when its capacity is required for adequate reserve alone, then, there has been an average excess of installed cable capacity above load that is about equal to the capacity of $1^1/_2$ cables. Stated in more general terms, this amounts to saying that any substation which has a growing load and is supplied by n cables will have an average of 1.5 cables excess above load hence the average ratio U of total installed emergency load capacity to the yearly maximum demand on the substation would be:

$$U = \frac{n}{n - 1.5} \qquad (1)$$

This ratio holds for any group of three or more identical lines, transformers, circuits, etc., so long as the number does not become large enough to justify consideration of coincident emergency loss of more than one unit in the group. It is always larger than would be expected by those who are much more accustomed to the lower instantaneous ratio u of emergency to normal loads:

$$u = \frac{n}{n - 1} \qquad (2)$$

Few substations are supplied by more than seven lines or have more than four transformer banks and some have only two. While it is true that the number of primary distribution circuits is large, it is seldom that the load of one circuit can be transferred to more than three adjacent circuits if it should fail ($n = 4$) and, similarly, it is often impractical to transfer the load of one distribution transformer to more than two adjacent transformers ($n = 3$). Hence, in each part of the distribution system, we find we are dealing with groups of units in which the values of (n) range from 2 to about 7. By substituting such values in equation 1, we may find directly that the resulting ratio of emergency load rating to the average yearly maximum demand per unit (table I) ranges from 4 to 1.27 for each such group. The corresponding values of the instantaneous ratio u are also shown.

Thus, there may be many substation supply cables in a given underground duct bank but, if the average number of supply cables per substation is four, the sum of the normal noncoincident maximum demands on all the supply cables in that duct bank probably will not reach a total greater than 62.5 per cent (100 per cent/1.6) of the combined emergency load ratings of those same cables. The same general relationship operates inexorably for each other class of load-carrying equipment in a growing distribution system and, to some less extent, in the bulk-power system.

It is possible that economy in load-carrying facilities may be improved to some extent, however, by judicious application of emergency load ratings that are somewhat higher than the load ratings which would represent sound engineering practice for regular daily operation. Equation 1 is also helpful in illustrating the maximum ratio of emergency to normal load ratings which would be required in order to bring the average yearly maximum demand up to a value equal to the installed normal load rating of each group of two or more units normally in service. Higher ratios have no practical value. Ratios as high as 1.6 and 2.0 would represent hopeless requirements, however, if emergency loads and their effects upon operating temperatures did not differ materially from normal load conditions.

We may, therefore, conclude from the foregoing discussion that the average cable loading and load losses in any given duct run are materially lower than might be expected if load growth and the usual provisions for continuous supply were not present. The emergency load losses lie in the range of 1.4 to 4.0 times the normal load losses, or in such a relative magnitude as to require special consideration. Further, the severity of the all-day emergency load occasioned by failures of

Table I

Number of Units Normally in Service	Average Ratio (U) Emergency Load Rating to Average Yearly Maximum Demand per Unit	Instantaneous Ratio (u) of Emergency to Normal Load
2	2 to 4.0*	2.00
3	2.0	1.50
4	1.60	1.33
5	1.43	1.25
6	1.33	1.20
7	1.27	1.17

* Note: Formula not entirely satisfactory for this case.

load-supply elements in service decreases materially as to the number of elements per group increases. Hence, the need for high emergency ratings practically disappears as the number of cables exceeds five or six per substation.

An ordinarily practical engineering approach to increased load ratings would be to determine first what maximum normal load rating may be permitted under the circumstances, then consider whether the probable emergency-load transients will be sufficiently frequent or severe to require that they, rather than the normal load conditions, determine the need for installing additional capacity. Emergency operations differ from normal conditions chiefly in two respects:

1. Emergency loads are transient in nature and, usually, short lived.
2. The probability is usually very remote that an emergency will occur at the time when loads, ambients, and the limitations of installed capacity would combine to require operation of the equipment at the highest possible temperature which is selected as the design limit.

Nearly all loads are variable in their nature rather than constant. Hence, the maximum temperature reached lasts for a very short time and is in most cases considerably lower than would obtain if the usually appreciable thermal capacity of the apparatus were not present to reduce the temperature changes that accompany changes in load.

The problem of emergency load ratings, therefore, reduces to consideration of such factors as the operating temperatures to be expected and their possible relative effect upon the life of the apparatus in question, as determined not only by the temperature-life characteristics of the apparatus, but also the probability of occurrence of transient loads sufficient in magnitude to require operation at above rated temperatures, or with abnormal voltage regulation.

Approximate Frequency of Cable Failures in Service

Perhaps to a greater extent than for any other equipment, the cable engineer has access to a sufficient volume of data to determine with reasonable assurance the probable failure rate for any cable or group of cables. There are, for instance, about 15 cable sections and joints per mile of an underground cable installation, within which distance there may be exposure to a wide variety of conditions such as hot spots, electrolysis, water, abrasion, bending, etc. Relatively few miles of such construction are therefore needed to give the law of averages a reasonably good chance to work.

In any given system, the deviations from the average failure rate usually may be explained. The author is familiar with an instance in which 22 miles of 13.2 kv cable were recently operated for about three years at very nearly twice the former average daily load and at maximum conductor temperatures ranging from 90 to 105 degrees centigrade. The failure rate averaged $4^1/_2$ times the prior ten-year average for this cable, but the station economies made possible by such extraordinary operation far outweighed the increased cost of cable maintenance. By far the chief causes of cable failure in this instance were sheath cracks at cable bends in the manholes or sheath abrasion over rough spots in the duct, both of which were due to the longitudinal movement of the cable. Only one failure occurred in a section of cable that showed definite signs of insulation injury by the inordinately continued repetition of high-temperature operation. There are no doubt other such cable sections that have not come to light and might ultimately accelerate the failure rate but the usual sheath crack was the direct cause of this failure. From these observations there is some indication that, for less severe conditions, the cable failure rate should be expected to increase almost in direct proportion to the thermal movement of the cable, or in proportion to the square of the load carried.

We can now estimate roughly the probable failure rate for cables supplying a given substation. Consider a group of five parallel supply cables, operating regularly at 80 per cent of their combined normal load rating, in which a cable failure may be expected about once in 450 load cycles. If the load were allowed to increase until the five cables would operate regularly at 100 per cent of their normal load rating, the average failure rate due to normal load operation might increase to about $(100/80)^2$ times the previous rate, or one in 290 load cycles. Should a failure then require operation over one load cycle in 290, at a failure rate that is ten times as severe as for the average normal load cycle, the over-all failure rate might further increase to one in 280 load cycles. In such an instance, the chief cause of the increased failure rate, therefore, would appear to be the increased normal loads and temperatures.

Similar analyses in respect to other types of equipment apparently develop similar results. In some cases it may be well to recognize the appreciable life loss due to all causes other than temperature, such as obsolescence, natural old age, mechanical damage, etc. In some instances this may be found to contribute as much to the average rate of life loss as do normal load temperatures. If the magnitude of this factor can be judged, its effect upon the existing rate of life loss should be deducted before estimating the change in life loss chargeable to a change in operating temperatures.

Distribution Load Characteristics

Seldom in the distribution system does the load correspond to the conditions which determine the name-plate rating of the load-carrying apparatus, or the conditions often assumed in establishing load ratings for cables. In some respects questions of seasonal load variations, as well as the daily load changes, involve conditions so complex that it is almost impossible to subject this phase of the problem to rigid analysis. Furthermore, the present growth of electric-range, off-peak, and air-conditioning load introduces factors which have a profound influence upon the shape of the daily load cycle. The development of such loads already has reached the point where the load curve at one location in the distribution system may bear very little direct relationship to that at another. It is no longer possible to select a load cycle that may be said with assurance to be typical or representative of any location except that where it was encountered. This becomes increasingly evident in the smaller supply units of the distribution system, such as substation circuits, distribution transformers, and low-voltage circuits where nearly all degrees of relative saturation of these new loads already exist.

To some extent, however, the range of load variation in a given case may be judged from load curves such as in figure 2*a* for all-day loads, or in figure 2*b* for short-time loads. These illustrate respectively the distribution of normal daily maximum demands and hourly loads throughout the year on one substation. All extraordinary loads were excluded from the data used in their preparation. Substations having identical load factors may have different load curves, although the area under the solid curve of figure 2*b* is proportional to annual load factor. Such curves become quite helpful for use in analyses to determine the approximate probability of meeting given load, voltage, or temperature conditions. The dotted curves shown in these figures illustrate the corresponding loads at each of five levels while the yearly maximum demand is growing, say, from 80 per cent

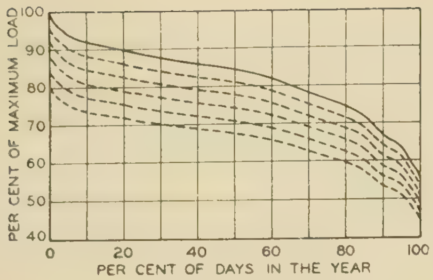


Figure 2a. Illustrative occurrence of daily maximum demands on supply cables, for yearly peak loads ranging from 80 per cent to 100 per cent of normal load rating

to 100 per cent of the installed load rating in a given case.

Approximate Probability of Emergency Load Occurrence

The probability of load occurrence is one of the most important factors in consideration of emergency ratings. Three truths must be appreciated in dealing with this factor, however. First, the necessity for dealing with averages makes the conclusion no better than the average from which it is derived. Second, an occurrence that may be expected no oftener than once in 100 years may confront the calculator within a month. Third, a loading condition which may be expected only once in 25 years in a given group of cables will be faced on an average of twice a year in a system which has 50 such groups of cables. These, however, serve only to emphasize the importance of selecting design limits for emergency operation that are no higher than the proponent is willing to face.

One use of probability will now be illustrated by a sample calculation for five cables supplying a distribution substation. For example, assume a supply-cable failure may be expected on an average of 1.4 times per year and that five years is required for the peak load on that substation to increase from 80 per cent to 100 per cent of the normal peak load capacity on all five cables. To be satisfied with such a design, the engineer must be willing to face the possible need for carrying an emergency load that can reach a maximum of 1.25 times the normal load design limit, should one cable be out of service at the time of the peak in the last year. Let us assume this is reasonable, and calculate the probability that an all-day outage of one cable would require the others to carry a peak load as high as 1.10 times their normal load rating. This will occur whenever the load on the substation would exceed 88 per cent of the total installed normal load rating of the five cables. By reference to curves such as shown in figure 2*a*, it may be found that the normal daily maximum demand in this instance will not exceed 88 per cent of rating at any time during the first or second years, but will during 3.2 per cent of the third year, 11.2 per cent of the fourth year, and 28.5 per cent of the fifth year, or an average of 8.58 per cent per year. With 1.4 cable failures per year uniformly distributed, then, the probability of cable failures during daily load cycles whose normal maximum loads exceed 88 per cent of rating becomes 0.0858 times 1.4 or 0.120. In this instance, then, all-day cable outages might be expected to require the remaining cables to operate at loads above 110 per cent of normal load rating on an average of once in about eight years. Other points may be calculated similarly and have been shown in the lower curve of figure 3. Should one-half day be ample to repair a cable fault, the corresponding time intervals for this curve would double.

For short-time emergencies the method of calculation is about the same as for the case just illustrated and may give a result quite like the dotted curve of figure 3. However, if figure 1 should represent the probable duration of such short-time emergencies, it is apparent that only one in seven will last as long as one-half hour. Hence, the probable interval between occurrence of loads requiring one-half hour or longer operation at the given or a greater load becomes seven times as long as the probable interval between the short-time emergencies. This latter relationship is also shown in figure 3.

As earlier pointed out, such calculations should not be used as a basis for making close decisions. These calculations, for instance, do not recognize the certainty that unbalance of loads between cables may load some heavier than others, etc. However, such curves become quite helpful in judging the maximum amount of life loss which is reasonable to accept, in selecting the design limits for emergency and normal load ratings. In the instance just used for illustration, the author considers the emergency-load design limits sufficiently conservative to permit their use as often as once each year, without any apparent effect upon cable maintenance costs and without any ominous increase in the likelihood of such operation causing a second cable failure during such a state of emergency.

The foregoing discussion can serve only to point out some of the factors which work together to make the probability of severe loads and temperatures much more remote than the apparatus engineer is apt to realize. Variations in the rate of load growth seem not to have an important effect upon the answer. Neither is it necessary to have an accurate knowledge of the failure rate since variations in the range of two to one can only halve the number of years between the occurrence of a given or higher load. In any event it is apparent that the probability of severe loading conditions is quite remote.

Since the major purpose of emergency ratings is to permit increasing the average-day loading of the load-carrying elements in the system and this may increase voltage regulation, as well as operating temperatures, it becomes necessary to give some passing thought to voltage.

Voltage Regulation

Voltage regulators can be made to take care of increased regulation elsewhere and, with present available methods, can also reduce the spread between the first and last customers on the circuit. Small fixed boosters, shunt capacitors, and step-type regulators are now available for use by those who find that increased load ratings introduce normal-voltage regulation problems that cannot be solved as well by orthodox copper addition or rearrangement.

With the gradual extension of some form of the network principle beyond the substation bus and farther out into the distribution system, emergency voltage regulation, however, becomes one of increasing importance. This is particularly true of aerial distribution. It is the more urgent because the number of parallel paths normally available for the supply of the load in this part of the system often is as few as two, hence the instantaneous ratio of emergency to normal load approaches values as high as 2.00.

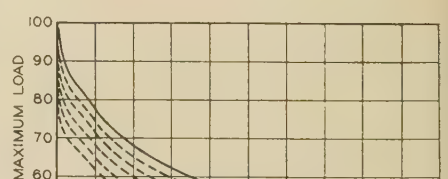
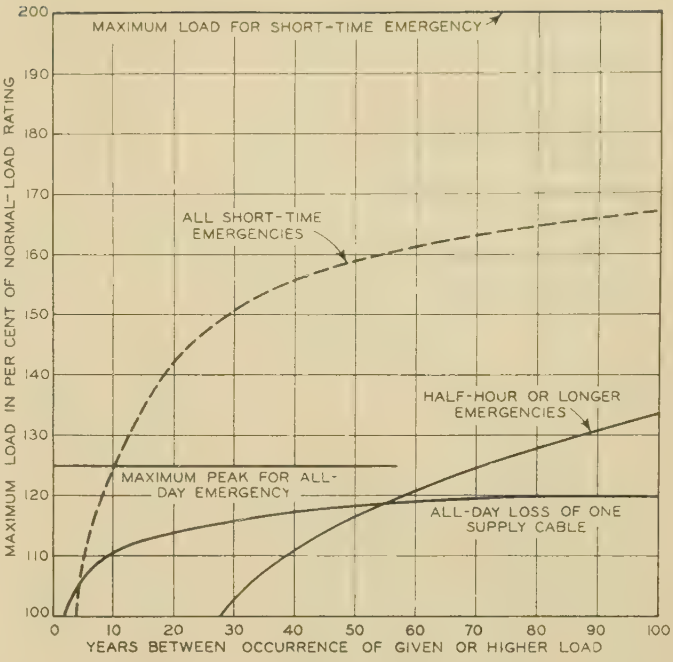


Figure 2b. Illustrative substation supply-cable load duration, for peak loads ranging from 80 per cent to 100 per cent of normal load rating

The principal gain possible by "networking" is its contribution to increased service continuity. This, however, is a voltage problem in its own right, since service discontinuity is the simple case of no voltage at all. To illustrate further, let us assume that there are two circuits supplying adjacent areas with voltages which lie outside the established limits of normal voltage variation only when one of the circuits fails to deliver any voltage at all, while trouble-men labor to clear the fault. By suitably "networking" these two circuits it may be possible to make the "no voltage" occasions much less frequent. The cost may well be unreasonable, if not almost prohibitive; however, should the design be made sufficiently rigid to prevent any possibility of ever delivering to the last customer a supply voltage that lies outside the accepted limits for normal operation.

Figure 3. Approximate probability of load occurrence for five cables supplying one substation



This raises the rather natural question whether it may not be good business to improve service continuity just so long as no step thus taken will result in delivering to the last customer less voltage than he needs to permit his refrigerator motor to start; should its thermostat call for power in the rare instance where none would be available if the network had not been provided to supply no less than the bare necessities, until conditions can be restored to normal. The probability that apparatus failures will conspire with loads to precipitate such a situation, during the hour or less required to clear the fault in such instances, may be determined without much difficulty, as for figure 3. Thus, this phase of emergency ratings would appear to suggest the occasional need for considering some design limits for emergency voltage regulation that may be quite apart from those for normal operation. As in temperature considerations, it may often be found that conditions necessary to satisfy the normal voltage requirements will need no assistance to keep utilization equipment operating in the rare instance when an emergency may cause temporarily abnormal voltage regulation at some extreme locations.

Temperature Transients

Contrary to the author's expectations, when he approached the emergency-rating problem several years ago, it has become increasingly evident that the probable interval between occasions when it would be necessary to operate at high emergency temperatures is often so long that the question of whether such an operation would cause 10 or 300 times the normal rate of life loss for the day in which it should occur reduces to one of comparative unimportance. Even as high a rate as 300 times normal life loss over one load cycle may be no more severe than one year of normal operation. Montsinger's evidence[2] that insulation life loss may in some cases double for each 8-degree-centigrade increase in temperature, however, seems to be sufficient to indicate that temperature calculations used as the basis for emergency ratings may occasionally require a practical answer that lies somewhere within 10 degrees or 20 degrees centigrade of the actual. The accumulation of sufficient data in practical operation to indicate which of the present theories of life loss is best may require some time. It will require reasonably accurate knowledge of the temperatures responsible for that life loss.

Published methods for direct calculation of operating temperatures for cables, transformers, etc., are available. Mathematical analysis, however, can give no better answer than is permitted by the accuracy with which the various thermal coefficients and temperature corrections are known. These often cannot be evaluated deductively without disquieting assumptions and considerable labor. Hence there appears to be some need for a little better general understanding of means which permit the engineer to determine empirically from test or other available data, all the coefficients needed to estimate the thermal response of the assembly in question to any regular or irregular transient load impulse. This can be accomplished by the use of an equivalent circuit which is not materially different from that usually assumed in calculations of steady-state load temperatures. Since load transients require consideration of copper losses only, it is helpful to treat the hot-spot temperature θ in two components:

1. The constant component or "threshold temperature" T_0 which is the sum of the ambient temperature and the steady-state temperature rise caused by such factors as no-load losses and that part of the copper-temperature rise which corresponds to the average daily copper losses of the cable itself and its associates in the given conduit run.
2. The variable component θ_1 caused by the instantaneous difference p between the impressed copper losses and those copper losses, if any, which contribute to the "threshold temperature."

The "threshold temperature" component may be developed exactly as the calculator has been in the habit of estimating normal-load temperatures. This method of approach makes it necessary to know the transient load characteristic of the assembly for a total period equal only to the maximum probable duration of the emergency. There is seldom need to deal with transients which persist longer than one day.

Now, let us consider the thermal circuit for the dissipation of the copper (variable) losses through the various successive steps encountered in the transfer of these losses from the conductor out to ambient. In a cable installation, for instance, there is a series of successive steps, such as copper, insulation, sheath, etc., which can be represented by the electric-circuit analogy of figure 4. This approaches an exact representation, as the steps become smaller. In this circuit the thermal dissi-

pation coefficients s_{12}, s_{23}, . . . , s_n are analogous to electrical conductances, while k_1, k_2, . . . , k_n are analogous to electrical capacitances. The instantaneous copper-loss input p has the nature of an electric current and the temperature rise θ_1 is the resultant "voltage" rise needed at the sending end of the circuit to cause "current" p to flow. As in most electric circuit analyses we shall tentatively disregard the effects of temperature upon the s coefficients of thermal dissipation and upon the electrical resistance component of the copper loss input p.

The circuit of figure 4 is useful principally to illustrate the general form of the natural response to an impulse. From his familiarity with electric-circuit theory, the reader may recognize the sending end response of this circuit as that given in equation 3, for the condition that the input p is held constant at some value P:

$$\theta_1 = AP(1-\epsilon^{-at}) + BP(1-\epsilon^{-bt}) + \ldots + NP(1-\epsilon^{-nt}) \quad (3)$$

Those who do not recognize this response may observe the development of that for a "two-chunk" circuit, in appendix B. Theoretically, each additional step in the circuit of figure 4 will require an additional term in equation 3. Where there are many steps, or the circuit constants are distributed rather than lumped, it is often necessary to use some mathematical expedient to evaluate equation 3. In his analysis of radial heat flow, for instance, Church[4] has shown how it is possible to obtain an expression for the harmonic impedance function for each homogenous step in the thermal circuit and, from these

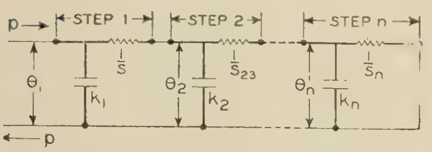


Figure 4. Thermal circuit

data, to determine the position of each of several points along the rectangular impulse characteristic. He has used a method so powerful that it can digest nearly any combination of steady-state harmonic functions that will represent the circuit at hand, and will produce the numerical position of any desired point along the curve of its response to a rectangular impulse. This method of harmonic analysis gets the answer, one point at a time, but seldom gives the response directly in its natural algebraic form. By whatever expedient the various points are evaluated, however, the characteristic response of the thermal circuit in figure 4 has the algebraic form given in equation 3.

It has been known for some time that only two exponential terms are needed to represent quite accurately the over-all thermal response of all the parts in an oil-filled transformer assembly. Similarly, it is known that aerial conductors, disconnects, and other assemblies whose current-carrying parts are sufficiently exposed to ambient temperatures, have thermal responses that may be quite accurately represented by the use of only one exponential term. Consequently, it does not seem to require undue use of the imagination to conclude that the form of equation 3 may be found to represent the natural heating characteristic for any situation to be encountered in electric power distribution. In any event, it should be possible to judge approximately the extent to which this is true throughout the range of a given set of observations. Let us now consider briefly the manner in which as few as two equivalent parts of such a circuit may be made to represent the infinite series of figure 4 and the following simpler equation:

$$y = \theta_1/P = A(1-\epsilon^{-at}) + B(1-\epsilon^{-bt}) \quad (4)$$

In appendix A there is developed a method for determining the effective values of the parameters A, a, B, and b, which will make equation 4 approximately fit any group of test observations for a given rectangular load impulse. In table II are shown the values of the parameters used in plotting the four curves drawn in figure 7, for underground cables. The curves are drawn to show the loci of equation 4 with respect to the observed points, also shown for comparison along the heating characteristics. In this case the observations shown were calculated by Church's method (AIEE TRANSACTIONS for 1931, page 982, and for 1935, page 1166) as modified to recognize some additional features to which the author's discussion of Church's latter paper called attention (AIEE TRANSACTIONS for 1936, page 398).

Such faithful correlation, in figure 7, for the difficult case of the underground cable, has led the author to conclude that equation 4 can represent the rectangular load impulse characteristic of any assembly, for periods up to 24 hours duration, with sufficient practical accuracy to leave little need for rigorous analysis when suitable test data are available or can be obtained. If this is true, there is promise for considerably simplifying calculations of the emergency temperature transients

Table II. Equivalent Thermal Circuit Constants for 13.2-Kv Three-Conductor 350,000-Circular-Mil Cable in an Underground Duct Bank

	Hottest of Eight Equally Loaded Cables		Hottest of Two Equally Loaded Cables	
	Cables Dry	Cables in Water	Cables Dry	Cables in Water
1. Empirical circuit constants (Appendix A)				
A	7.48	3.52	7.00	3.56
B	12.9	13.7	10.4	5.92
a	0.819	2.08	0.857	2.07
b	0.118	0.0304	0.136	0.0480
2. Approximate circuit constants (Appendix B)				
K_1	0.132	0.130	0.133	0.130
S_{12}	0.090	0.256	0.096	0.260
K_2	0.763	2.34	0.884	3.58
S_2	0.108	0.0753	0.143	0.178

which accompany other than rectangular load impulses.

The approximate circuit of figure 5 most nearly corresponds to the actual thermal circuit, but has no particular advantage over the empirical circuit of figure 6 which has about the simplest arrangement of thermal capacities and resistances that will give exactly the same sending-end response to all regular or irregular impulses. The coefficients for the empirical circuit of figure 6 are known immediately

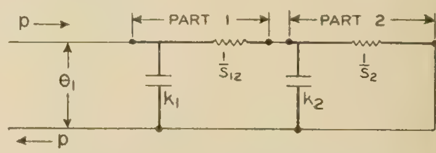


Figure 5. Approximate circuit

upon finding the parameters which make equation 4 fit the observed data, as by the method of appendix A. An advantage of the empirical circuit lies in the fact that the instantaneous input is the same for the A part as for the B part, while the instantaneous response of the A part is absolutely independent of that for the B part, so that:

$$\theta_1 = \theta_a + \theta_b \quad (5)$$

while,

$$\Delta\theta_a = (apA - a\theta_a)\,\Delta t \quad (6)$$

and,

$$\Delta\theta_b = (bpB - b\theta_b)\,\Delta t \quad (7)$$

For those who are sufficiently interested, appendix B gives the corresponding instantaneous relationships found in the approximate circuit of figure 5, as well as the relations between the equivalent thermal constants of this circuit and the parameters of the empirical circuit which has an identical response to all

transients. The following relations are included:

$$K_1 = \frac{1}{aA + bB} \tag{8}$$

$$S_{12} = K_1(a + b) - K_1^2 ab(A + B) \tag{9}$$

$$S_2 = \frac{S_{12}}{S_{12}(A + B) - 1} \tag{10}$$

$$K_2 = \frac{S_{12}S_2}{abK_1} \tag{11}$$

The calculator need know no more about the thermal characteristics of the assembly. Should he desire, he may now develop the approximate harmonic impedance function directly from figure 6, by analogy to the electric circuit of parallel resistance and capacitance. For instance, let $X_a = aA/\omega$ and $X_b = bB/\omega$, where ω equals $2\pi f$ radians per hour. Then the impedance $\boldsymbol{Z}$ of the empirical circuit becomes:

$$\boldsymbol{Z} = -j\left[\frac{AX_a}{A - jX_a} + \frac{BX_b}{B - jX_b}\right]$$
$$= \left[\frac{AX_a^2}{A^2 + X_a^2} + \frac{BX_b^2}{B^2 + X_b^2}\right] -$$
$$j\left[\frac{A^2X_a}{A^2 + X_a^2} + \frac{B^2X_b}{B^2 + X_b^2}\right] \tag{12}$$

However, equations 6 and 7 offer means for a simple and direct tabular calculation that avoids any need for dealing with complex numbers or breaking the instantaneous values of p down into the various harmonic components. For instance, assume that: $B = 24$ degrees centigrade ultimate rise due to rated-load copper losses, while $b = 0.24$ and that it is desired to calculate $\Delta\theta_b$ for quarter-hour intervals ($\Delta t = 0.25$ hour). Substituting these values in equation 7 gives the following guide for calculating the approximate temperature change across resistance B during each quarter-hour interval:

$$\Delta\theta_b = 1.43p - 0.060\theta_b \tag{7a}$$

Table III shows a sample calculation for one illustrative load cycle, for which the load value at the beginning of each interval has been expressed in terms of the ratio R of load to rating. In instances where the "threshold temperature" does not need to include any part of the copper losses, it is often convenient to express the instantaneous load values in times rating R since this method gives the maximum temperature rise directly in terms of rating. The effects of temperature upon the values of p or the coefficients of the thermal circuit seldom have any practical importance in such calculations, so long as the parameters of equation 4 are based upon observations at constant load throughout the range of temperature changes within which it is desired to operate. The parameters then include the average temperature correction. The tabular calculation of θ_a may be made exactly as that for θ_b. Their maxima will not be coincident, but questions of such time-phase displacement take care of themselves. The fact that neither θ_a nor θ_b has any practical significance if taken alone, is of no disadvantage. The algebraic sum of their instantaneous values is always equal to the hot-spot rise θ_1 above the "threshold temperature" T_0. When dealing with cable-temperature calculations, in which the instantaneous value of p equals the total copper losses less the average daily copper losses in that cable, the values of p will be negative whenever the total copper losses fall below the average daily copper losses, if the calculator should elect to investigate temperatures during light-load periods. This method, however, makes it usually not necessary to start tabular calculation

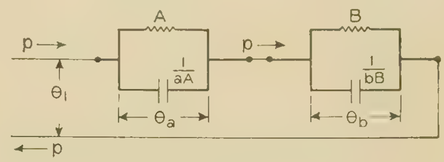


Figure 6. Empirical circuit

at a time when the copper losses are less than the average for a normal day.

The time interval Δt that is used in equations 6 and 7 for practical calculations of cable temperature, seldom needs to be less than one-fourth to one-half hour, depending chiefly upon the number of observations of p that are needed to give a fair representation of the variations in copper losses. Adequate recognition of the thermal circuit itself is apparently retained so long as the interval Δt thus selected is not much larger than $(A + B)/4(aA + bB)$. When the value of a is large and a/b is greater than nine as for many transformers, however, it is readily possible that a more convenient value of Δt will make $a\Delta t$ greater than three. When this is true θ_a from equation 6 will so nearly reach its ultimate value pA within each step as to permit assuming this simplification of equation 6, and using fewer steps.

While the foregoing method appears to be direct and convenient for transient-temperature calculations, it is only one of several. It has been helpful to the author, in occasional difficult situations and has been outlined with the thought it may be helpful to others who are not already familiar with a method that produces satisfactory results. Many time-saving opportunities will appear to the calculator, as he becomes more familiar with the method he elects to use.

A possible use of the circuits of figures 5 and 6 for underground cables lies in the ease with which they permit a direct though somewhat approximate estimate of what would be the corresponding thermal circuit for some other size or type of cable in the same situation. An inkling of this possibility may be had from table II in which it will be observed that K_1 has the same value in each of the four situations, where S_{12} changes only with the addition of water in the duct. For underground installations, therefore, part 1 appears to be the cable itself. The per-

Table III. A Sample Calculation Using Equation 7a as a Guide

Time (t)	Load (r) in Times Rating	1.43 p (1.43 r²)	−0.060 θ_b	$\Delta\theta_b$	θ_b	
4:00 p.m.	0.34 R	0.167 R^2	−0.167 R^2	0*	2.78 R^2	
4:15	0.55 R	0.432 R^2	−0.167 R^2	0.27 R^2	3.05 R^2	
4:30	0.57 R	0.465 R^2	−0.183 R^2	0.28 R^2	3.33 R^2	
4:45	0.69 R	0.682 R^2	−0.200 R^2	0.48 R^2	3.81 R^2	
5:00	0.71 R	0.720 R^2	−0.228 R^2	0.49 R^2	4.30 R^2	
5:15	0.74 R	0.785 R^2	−0.258 R^2	0.53 R^2	4.83 R^2	
5:30	0.86 R	1.06 R^2	−0.290 R^2	0.77 R^2	5.60 R^2	
5:45	0.95 R	1.29 R^2	−0.34 R^2	0.95 R^2	6.55 R^2	
6:00	1.00 R	1.43 R^2	−0.39 R^2	1.04 R^2	7.59 R^2	
6:15	0.99 R	1.40 R^2	−0.46 R^2	0.94 R^2	8.53 R^2	
6:30	0.92 R	1.21 R^2	−0.51 R^2	0.70 R^2	9.23 R^2	
6:45	0.95 R	1.29 R^2	−0.55 R^2	0.74 R^2	9.97 R^2	
7:00	0.89 R	1.13 R^2	−0.60 R^2	0.53 R^2	10.50 R^2	
7:15	0.82 R	0.960 R^2	−0.630 R^2	0.33 R^2	10.83 R^2	
7:30	0.82 R	0.960 R^2	−0.650 R^2	0.31 R^2	11.14 R^2	
7:45	0.80 R	0.918 R^2	−0.668 R^2	0.25 R^2	11.39 R^2	
8:00	0.83 R	0.985 R^2	−0.684 R^2	0.30 R^2	11.69 R^2	
8:15	0.71 R	0.720 R^2	−0.700 R^2	0.02 R^2	11.71 R^2	
8:30	0.74 R	0.785 R^2	−0.705 R^2	0.08 R^2	11.79 R^2	maximum
8:45	0.68 R	0.660 R^2	−0.71 R^2	−0.05 R^2	11.74 R^2	
9:00	0.62 R	0.550 R^2	−0.70 R^2	−0.15 R^2	11.59 R^2	

* NOTE: This is often a safe assumption, if load changes are small prior to the period under observation.

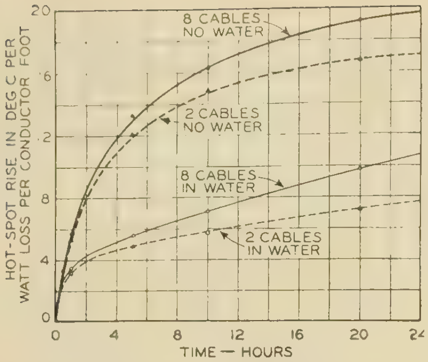


Figure 7. Hot-spot response to constant load losses, for groups of equally loaded 13.2-kv three-conductor 350,000-circular-mil cables in duct

centage change in the thermal capacity coefficient of the cable, also the corresponding percentage change in thermal conductance from copper out through the sheath, may be estimated in a few minutes. The effect of these changes in K_1 and S_{12} upon the parameters a, b, A, and B, may then be determined (appendix B) with almost the same confidence as though the original observations represented such cable.

A Word About Tests and Thermal Corrections

There are a few points regarding tests and thermal corrections that may be worthy of passing mention. Until one becomes thoroughly familiar with his data, it is usually best to express input in watts, thermal capacity in watt-hours per degree centigrade, thermal dissipation in watts per degree centigrade, and temperature rise in degrees centigrade per watt. Otherwise the test observations of rising temperature at constant load will appear to give a slightly faster moving response than similar observations after the load is removed. This is because of the greater acceleration caused by the effect of thermal increases in electrical resistance upon input in the former case. When using test data expressed in such units, there can be no confusion as to when to make some approximate final adjustment for the higher electrical resistance at the higher temperature. If the correction is excluded from the test data, it should be recognized in the calculation.

Where the hot-spot temperature cannot be read directly during a test on a given installation, the average copper temperature may be assumed to equal the hot spot, with sufficient accuracy for cable-temperature analyses. In oil-filled transformers, the hot-spot temperature at any time should be about equal to the average winding temperature plus the oil temperature rise from that at a level opposite the midpoint of the winding to that of the top oil. Thus it is usually possible to determine the approximate hot-spot temperature at each point of the test curve for cases where it cannot be read directly. It is seldom necessary to test any one assembly at more than one constant load value. If observations are taken on the heating curve, it may be helpful in some cases to continue them on through the cooling cycle after the load has been removed. In this way the one set of observations may be used as a check on the other.

Conclusion

These notes have been prepared with the thought that they will serve their purpose if they show in a general way some of the conceptions that should be included in a broad review of the opportunities to effect system economies by the judicious application of emergency ratings.

List of Symbols

A, B, etc.	=	parameters
a, b, etc.	=	parameters
f	=	harmonic frequency, in cycles per hour
K_1, K_2, etc.	=	thermal capacity coefficients, in (watts)(hours)/(degrees centigrade)
P	=	maximum value of copper loss transient, in watts
p	=	instantaneous value of copper loss transient, in watts
R	=	ratio of maximum load to rating
r	=	instantaneous load
S_{12}, S_2, etc.	=	thermal dissipation coefficients, in (watts)/(degrees centigrade)
T_0	=	threshold temperature, in degrees centigrade. It includes all constant components of the hot-spot temperature
t	=	time in hours
U	=	ratio of total installed emergency-load capacity to average yearly maximum demand
u	=	instantaneous ratio of emergency to normal load
x	=	dy/dt
y	=	thermal response (θ_1/P) to an impulse, in (degrees centigrade)/(watts)
Z	=	harmonic impedance of the thermal circuit
ϵ	=	base of Napierian logarithms
θ	=	hot-spot temperature (T_0 plus θ_1), in degrees centigrade
θ_1	=	variable component of hot-spot temperature, in degrees centigrade
θ_a and θ_b	=	arbitrary components of θ_1
ω	=	$2\pi f$ radians per hour

Appendix A. Empirical Analysis of Temperature-Time Characteristic for a Rectangular Load Impulse

Given the test curve of temperature, such as shown in figure 7, which represents the temperature-time characteristic of a given assembly when subjected to a rectangular load impulse. The problem is to find the values of A, B, a, and b, which when substituted in the equation:

$$y = A(1 - \epsilon^{-at}) + B(1 - \epsilon^{-bt}) \tag{13}$$

will make it fit the curve with reasonable fidelity for all values of t within the range of the observed data.

Select five points on the curve: one (t_1, y_1) which lies well up on the fast moving part of the curve; one (t_0, y_0) at $t = 0.5t_1$; and three others at about $t_2 = mt_1$, $t_3 = 2mt_1$, and $t_4 = 4mt_1$, in such a way that m is greater than 4, but $4mt_1$ includes no more than 80 per cent of the transient's apparent life. Graphically determine the slope x of the curve at each of the three latter points.

Now solve for b by considering the points (t_2, y_2), (t_3, y_3), and (t_4, y_4), which have been selected in such a way that they should be in the region where (at) is greater than 4, hence, for practical purposes,

$$y = A + B(1 - \epsilon^{-bt}) \tag{13a}$$

and

$$\frac{dy}{dt} = Bb\epsilon^{-bt} = x \tag{14}$$

From the observed slopes x_2, x_3 and x_4, then,

$$\frac{x_2}{x_3} = \epsilon^{b(t_3 - t_2)},\quad \frac{x_2}{x_4} = \epsilon^{b(t_4 - t_2)},\quad \frac{x_3}{x_4} = \epsilon^{b(t_4 - t_3)} \tag{15}$$

From these three relationships it is possible to write directly the value of b which most nearly satisfies these observations.

$$b = \frac{1}{3}\left[\frac{\log_n(x_2/x_3)}{t_3 - t_2} + \frac{\log_n(x_2/x_4)}{t_4 - t_2} + \frac{\log_n(x_3/x_4)}{t_4 - t_3}\right] \tag{16}$$

To solve for B substitute the value of b from equation 16, also (y_2), (y_3), and (y_4) respectively in equation 13a, as:

$$y_2 = A + B(1 - \epsilon^{-bt_2}),\ y_3 = A + B \times (1 - \epsilon^{-bt_3}),\ y_4 = A + B(1 - \epsilon^{-bt_4})$$

then

$$y_4 - y_2 = B_{24}(\epsilon^{-bt_2} - \epsilon^{-bt_4})$$

$$y_4 - y_3 = B_{34}(\epsilon^{-bt_3} - \epsilon^{-bt_4})$$

$$y_3 - y_2 = B_{23}(\epsilon^{-bt_2} - \epsilon^{-bt_3})$$

Hence

$$B = \frac{1}{3}\left(\frac{y_4 - y_2}{\epsilon^{-bt_2} - \epsilon^{-bt_4}} + \frac{y_4 - y_3}{\epsilon^{-bt_3} - \epsilon^{-bt_4}} + \frac{y_3 - y_2}{\epsilon^{-bt_2} - \epsilon^{-bt_3}}\right) \tag{17}$$

To solve for A, substitute B, b, and the observed values of y_2, y_3, and y_4 in equation 13a, as:

$$A_2 = y_2 - B(1 - \epsilon^{-bt_2})$$

$$A_3 = y_3 - B(1 - \epsilon^{-bt_3})$$

$$A_4 = y_4 - B(1 - \epsilon^{-bt_4})$$

$$A = \frac{A_2 + A_3 + A_4}{3} = \frac{y_2 + y_3 + y_4}{3} - B + \frac{B}{3}(\epsilon^{-bt_2} + \epsilon^{-bt_3} + \epsilon^{-bt_4}) \quad (18)$$

To solve for a, consider the points t_0 and t_1 between the origin and the apparent "knee" of the curve. In this region it should be found that ϵ^{-bt} is very nearly equal to $(1 - bt)$. Otherwise select more suitable points nearer the origin, then substitute the new values of (t_1), (y_1), and $(\epsilon^{-bt_1} = 1 - bt_1)$ in equation 13 as follows:

$$y_1 = A(1 - \epsilon^{-at_1}) + Bbt_1$$

similarly,

$$y_0 = A(1 - \epsilon^{-at_0}) + Bbt_0$$

and from these,

$$a = \frac{1}{2}\left[\frac{1}{t_1}\log_n\left(\frac{A}{A + Bbt_1 - y_1}\right) + \frac{1}{t_0}\log_n\left(\frac{A}{A + Bbt_0 - y_0}\right)\right] \quad (19)$$

Equations 16, 17, 18, and 19 permit evaluating all the constants needed to make equation 13 represent the actual conditions about as well as the data from which they were developed. A sample calculation follows.

Sample Calculation of A, a, B, and b for Hottest of Eight Equally Loaded Cables in Duct in Earth

Given:

Point	t(Hours)	y	(dy/dt) = x
	0	0	
t_0	0.5	3.34	
t_1	1.0	5.60	
t_2	5.0	13.30	0.978
t_3	10.0	16.30	0.434
t_4	20.0	19.20	0.190

$$b = \frac{1}{3}\left[\frac{\log_n 2.25}{(t_3 - t_2) = 5} + \frac{\log_n 5.15}{(t_4 - t_2) = 15} + \frac{\log_n 2.29}{(t_4 - t_3) = 10}\right] = 0.118 \quad (16a)$$

$$B = \frac{1}{3}\left[\frac{19.2 - 13.3}{\epsilon^{-0.59} - \epsilon^{-2.36}} + \frac{19.2 - 16.3}{\epsilon^{-1.18} - \epsilon^{-2.36}} + \frac{16.3 - 13.3}{\epsilon^{-0.59} - \epsilon^{-1.18}}\right] = 12.90 \quad (17a)$$

$$A = \frac{1}{3}[13.3 + 16.3 + 19.2 - 38.7 + 12.90(\epsilon^{-0.59} + \epsilon^{-1.18} + \epsilon^{-2.36})] = 7.48 \quad (18a)$$

$$a = \frac{1}{2}\left[\frac{1}{1}\log_n\left(\frac{7.48}{7.48 + 1.52 - 5.60}\right) + 2.0\log_n\left(\frac{7.48}{7.48 + 0.76 - 3.34}\right)\right] = 0.819 \quad (19a)$$

$$y = 7.48(1 - \epsilon^{-0.819t}) + 12.90(1 - {}^{-0.118t}) \quad (20)$$

A plot of equation 20, which is the upper curve drawn in figure 7, shows the manner in which it represents the observations from which it was derived.

Appendix B. Direct Analysis of the Two-Chunk Series Thermal Circuit with Constant Coefficients

The electric circuit which most nearly represents the relations between the usual thermal coefficients for the respective parts of the equivalent thermal circuits for transient load losses in transformers as well as cable installations, etc., is shown in figure 5 of this paper, in which (S_{12}) and (S_2) have the nature of conductances, (K_1) and (K_2) are thermal capacity coefficients, (p) has the nature of an electric current, and θ corresponds to the voltage rise required to cause "current" (p) to flow. Of the heat (pdt) flowing into part one, some $(K_1 d\theta_1)$ is absorbed by part one and the rest $(\theta_1 - \theta_2)S_{12}dt$ flows on into part two, hence:

$$pdt = (\theta_1 - \theta_2)S_{12}dt + K_1 d\theta_1 \quad (21)$$

and similarly, the heat entering part two $(\theta_1 - \theta_2)S_{12}dt$ is partly absorbed and partly dissipated through (S_2), or,

$$(\theta_1 - \theta_2)S_{12}dt = \theta_2 S_2 dt + K_2 d\theta_2 \quad (22)$$

By solving (21) and (22), first to eliminate (θ_2) and then (θ_1), the two following instantaneous relationships are obtained, one in terms of the hot-spot rise (θ_1) and the other in terms of (θ_2):

$$\frac{d^2\theta_1}{dt^2} + \left(\frac{S_{12}}{K_1} + \frac{S_{12} + S_2}{K_2}\right)\frac{d\theta_1}{dt} + \frac{S_{12}S\,\theta_1}{K_1K_2} = p\left(\frac{S_{12} + S_2}{K_1K_2}\right) \quad (23)$$

and

$$\frac{d^2\theta_2}{dt^2} + \left(\frac{S_{12}}{K_1} + \frac{S_{12} + S_2}{K_2}\right)\frac{d\theta_2}{dt} + \frac{S_{12}S_2\theta_2}{K_1K_2} = p\left(\frac{S_{12}}{K_1K_2}\right) \quad (24)$$

When the values of the S and K coefficients are constants and $p = P$ one solution for each of these linear equations takes the following form:

$$\theta = AP(1 - \epsilon^{-at}) + BP(1 - \epsilon^{-bt}) \quad (25)$$

In either case the values of a and b will be as follows:

$$a = \frac{1}{2}\left(\frac{S_{12}}{K_1} + \frac{S_{12} + S_2}{K_2}\right) + \sqrt{\frac{1}{4}\left(\frac{S_{12}}{K_1} + \frac{S_{12} + S_2}{K_2}\right)^2 - \frac{S_{12}S_2}{K_1K_2}} \quad (26)$$

$$b = \frac{1}{2}\left(\frac{S_{12}}{K_1} + \frac{S_{12} + S_2}{K_2}\right) - \sqrt{\frac{1}{4}\left(\frac{S_{12}}{K_1} + \frac{S_{12} + S_2}{K_2}\right)^2 - \frac{S_{12}S_2}{K_1K_2}} \quad (27)$$

while the values of A and B which make equation 25 represent θ_1, are

$$A = \left(\frac{1}{a - b}\right)\left[\frac{1}{K_1} - b\left(\frac{1}{S_{12}} + \frac{1}{S_2}\right)\right] \quad (28)$$

$$B = -\left(\frac{1}{a - b}\right)\left[\frac{1}{K_1} - a\left(\frac{1}{S_{12}} + \frac{1}{S_2}\right)\right] \quad (29)$$

Conversely, if equation 25 accurately represents test observations of the hot-spot temperature (θ_1), the effective values of K_1 K_2, S_{12}, and S_2 become:

$$K_1 = \frac{1}{aA + bB} \quad (8)$$

$$S_{12} = K_1(a + b) - K_1^2 ab(A + B) \quad (9)$$

$$S_2 = \frac{S_{12}}{S_{12}(A + B) - 1} \quad (10)$$

and

$$K_2 = \frac{S_{12}S_2}{abK_1} \quad (11)$$

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Discussion

R. J. Woodrow (Philadelphia Electric Company, Philadelphia, Pa.): In his analysis Mr. Kidder has shown that the approximate thermal circuit given by figure 5 of his paper can be replaced by and used interchangeably with the empirical circuit of figure 6 in order to analyze the temperature rise θ_1, and has given equations in appendix B by which the constants of either circuit can be calculated from those of the other. This is particularly valuable in investigating the thermal characteristics of a number of types of electrical equipment such as transformers, regulators, oil circuit breakers, and cables, whose thermal heating circuits are for all practical purposes composed of two parts as shown in figure 5.

In order to determine rapidly and simply the constants a, b, A, and B of the empirical

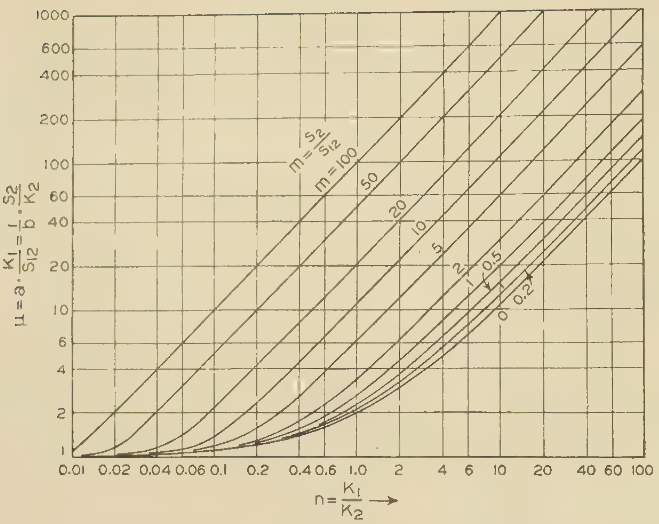

Figure 1

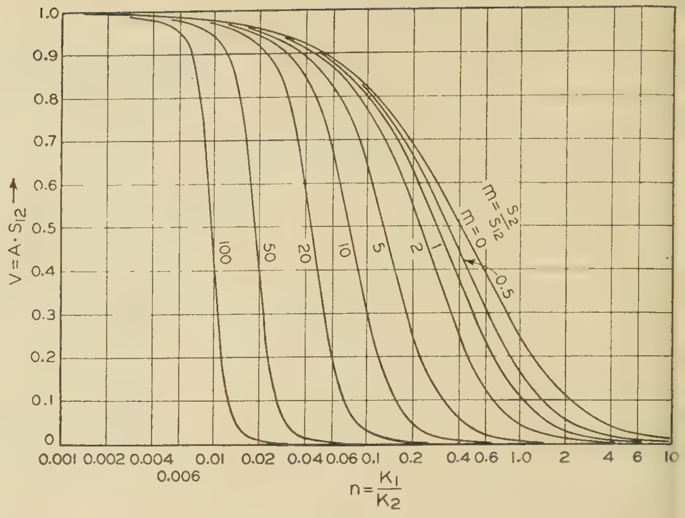

Figure 2

circuit of figure 6 of Mr. Kidder's paper from the constants S_{12}, S_2, K_1, and K_2 of the circuit of figure 5, three sets of curves have been drawn and are shown in figures 1, 2, and 3 of this discussion. By entering these curves with $n = K_1/K_2$ and $m = S_2/S_{12}$, A and B can be determined directly in per unit of the corresponding thermal resistances $1/S_{12}$, and $1/S_2$ of figure 5, and a and b in per unit of the corresponding reciprocal time constants S_{12}/K_1 and S_2/K_2. The relations which apply when using the curves are as follows:

Enter curves with $n = \dfrac{K_1}{K_2}$ and $m = \dfrac{S_2}{S_{12}}$

Read values of U, V, and W.

Then

$$a = U \cdot \frac{S_{12}}{K_1} \qquad b = \frac{1}{U} \cdot \frac{S_2}{K_2}$$

$$A = V \cdot \frac{1}{S_{12}} \qquad B = W \cdot \frac{1}{S_2}$$

As stated by Mr. Kidder the temperature differences θ_a and θ_b of the empirical circuit of figure 6 have no real significance and actually do not exist in the equipment. There are some cases, however, in which these temperature differences are so nearly the same as those that actually do exist across parts 1 and 2 of the circuit of figure 5 that, practically, they may be assumed to be the same. From the curves it may be seen that when $n = K_1/K_2$ is less than 1/50, and $m = S_2/S_{12}$ is less than 1/1, the constants of the empirical circuit and those of the actual circuit do not in any case differ by more than five per cent. Thus in an oil-filled transformer, for example, where the above limiting values of m and n are somewhat representative, there is not much error in considering the thermal circuits as if the constants of figure 5 were identical to those for figure 6 (that is, $U = V = W = 1.0$).

Equation 4 of Mr. Kidder's paper can be rewritten slightly as follows to give the temperature θ_1:

$$\theta_1 = P[A(1 - \epsilon^{-at}) + B(1 - \epsilon^{-bt})]$$

in which there are two exponential terms with different time constants. It should be pointed out that not only does the solution for θ_1 have two exponential terms, but θ_2, the temperature rise of part 2, and $\theta_1 - \theta_2$, the temperature difference across part 1 of figure 6, also have two exponential terms with the same time constants in their solutions. Thus:

$$\theta_2 = \frac{P}{S_2}\left[1 - \frac{1}{a-b}\left\{a\epsilon^{-bt} - b\epsilon^{-at}\right\}\right]$$

$$\theta_1 - \theta_2 = \frac{P}{S_{12}}\left[1 - \frac{1}{a-b}\left\{\left(\frac{S_{12}}{K_1} - b\right)\epsilon^{-at} + \left(a - \frac{S_{12}}{K_1}\right)\epsilon^{-bt}\right\}\right]$$

Again taking transformer heating as an example, this means that the top oil temperature rise (θ_2) has both a slow-moving and a fast-moving term in its response to a steadily applied load, the fast-moving term being opposite in sign to that of the slow-moving term and thus causing θ_2 to rise less rapidly for a short time after the load is applied. Likewise in the temperature rise ($\theta_1 - \theta_2$) of winding above top oil temperature there are both fast-moving and slow-moving exponential terms. However as previously pointed out, with n approximately 1/50 and m approximately 1/1, the error is not large in considering the top oil temperature as having a single slow-moving term in its response, and the winding rise above top oil as having a single fast-moving term in its response.

Although it is approximately correct to assume that the heating of an assembly having a thermal circuit such as given by figure 5 of Mr. Kidder's paper is practically the same as given in figure 6 with no change in constants if n is less than 1/50 and m less than 1/1, when these ratios increase the error rapidly becomes larger. For example if $K_1 = K_2$ and $S_{12} = S_2$ (that is, $m = n = 1.0$), then the following values result:

$$a = 2.62 \cdot \frac{S_{12}}{K_1} \qquad b = 0.38 \cdot \frac{S_2}{K_2}$$

$$A = 0.11 \cdot \frac{1}{S_{12}} \qquad B = 1.89 \cdot \frac{1}{S_2}$$

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): I wish to comment on the statement made in the last paragraph preceding the conclusion to the effect that in oil-filled transformers the hot-spot temperature at any time should be about equal to the average winding temperature plus the difference in temperature between the top oil and the oil opposite the midpoint of the winding.

When first reading this rule, I felt that it would not be a safe rule to follow under heavy overload conditions. Further investigation, however, showed that in general it gives approximately correct results up to 150 per cent load. Nevertheless, I wish to qualify the statement about its being a safe rule to follow under all conditions. While

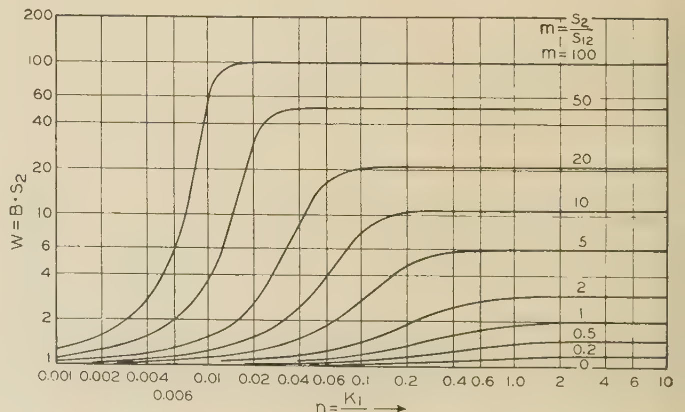

Figure 3

there is a certain relationship between the hot-spot temperature (and by hot spot I mean the difference between the maximum and the average winding temperatures) and the difference between the top oil and average oil temperature, factors other than the vertical temperature gradient affect the hot-spot temperature. One of these factors is current density in the copper coupled with the type of coil. One design may have a low current density, while another design has a high current density, yet both may have the same vertical oil gradient. The hot spots may be quite different in the two designs.

Another factor which the rule does not take into consideration is eddy-current losses which may not be uniformly distributed throughout the windings. Hot-spot temperatures will in general range from four or five degrees to ten degrees at normal load, the average probably being about seven degrees.

To see how the rule worked I took a typical case assuming that at normal load both the vertical oil gradient and the hot spot was seven degrees and found that at 125 per cent the rule was in error by approximately one degree (too low) and at 150 per cent the rule was in error approximately two degrees. This is close enough for all practical purposes.

On the other hand, if the hot spot is ten degrees at normal load and if the oil gradient is seven degrees, the rule, when used for 125 per cent load, would be in error by approximately five degrees. For 150 per cent load the error would be approximately eight degrees.

Since the rule covers only the vertical oil gradient factor that affects the hot-spot temperature, some care should be used in applying it in the field. It is easy to use if the user knows where the midpoint of the winding is by placing two thermometers on the outside of the tank—one at the top and one opposite the midpoint of the windings.

E. R. Thomas (Consolidated Edison Company of New York, Inc., New York): Mr. Kidder is to be congratulated in assembling together the factors to be considered in designing a transmission and distribution system. In obtaining equations 1 and 2, I assume that the derivation is based only upon a first contingency. As the number of units increases numerically to values greater than seven, as for example in the number of feeders being considered into an a-c network area, operating experience has shown that it is not uncommon to have a second contingency. I would like to ask the author whether, in his opinion, consideration should be given to second contingencies as affecting the average ratio of emergency load rating to normal rating when five or more units are being treated as a design combination.

F. H. Buller (General Electric Company, Schenectady, N. Y.): Mr. Kidder's primary purpose in presenting this paper appears to be the development of a method whereby the efficiency of utilization of existing plant may be increased. It is evident that considerable savings will result if it is feasible to carry increased loads on existing plant without overloading the aforesaid plant, and Mr. Kidder proposes to do this by what amounts to a material reduction in the amount of spare capacity available for use during emergencies.

This of course may result in the overloading of the plant when such emergencies actually arise; and Mr. Kidder has attempted to discover just about how much such overloads are likely to damage the existing cable.

He has shown in his paper that an overload of approximately 25 per cent, as compared with the maximum the cable should carry according to the AIEE temperature rules, will produce a very material increase in cable failures. Just what proportion of the $4^1/_2$ times figures which he gives can be directly attributed to the overload, cannot be established very clearly without knowing the peak loads which the cable was carrying in the ten years prior to the application of the long-time overload; but it must be a very considerable proportion of the figure in question.

Furthermore, Mr. Kidder points out that the failures which have resulted from the overload to date have almost all been sheath failures, caused by abrasion, or by bending of the cable in the manhole, particularly in the neighborhood of the duct mouth. We would expect his failure rate to increase materially with time rather than to decrease for two reasons. First, the lead sheath is materially weakened by high temperatures; and while three years of operation would probably weed out most of the weak spots in the lead sheath, and also the spots which are materially overstressed due to repeated bending as compared with the rest of the cable, it is possible that other weak spots have been developed in the sheath of the remaining cable due to the high temperatures, over and above those which have been found already; and these weak spots might never have existed had the cable been operated at a more normal temperature. Moreover, the bends in the manholes at the duct mouths are still in operation and will probably contribute just as much to the cable failure rate as they ever did, if the high-temperature load cycles are maintained.

Second, the wide range of temperature which is encountered on these cycles will unquestionably cause considerable sheath stretching and compound migration even where no sheath failure actually occurred. This means that void formation and ionization will be much more severe under these wide temperature cycles than would be the case if the AIEE limitations were respected. Now void formation and ionization usually tend to work quite gradually in producing cable failures, unless they are extremely severe from the beginning; and the fact that few failures have occurred as yet due to deterioration of the insulation does not mean that a large number of failures may not occur in the future due to these causes.

With regard to the mathematical derivation given in appendix *B* and covering a direct analysis of the two-chunk series thermal circuits, this analysis should probably work out pretty well for low-voltage cables and for cables of intermediate voltage.

Mr. Kidder points out that for overhead lines without insulation that one exponential term is sufficient. The reason for this is self-evident. The thermal resistance of the copper in such a circuit is exceedingly low, and there is no thermal resistance of the insulation; consequently all the thermal resistance is concentrated at the surface of the conductor, and is therefore actually a concentrated constant. The thermal capacity of the copper is the only thermal capacity involved in the heat dissipation of a system of this sort; and this is also for all practical purposes a concentrated constant. For two concentrated constants of this type a single exponential term is sufficient to represent the performance of the system mathematically, just as a single exponential term is sufficient in an electrical circuit containing concentrated resistance and capacitance.

If the insulation of the cable possesses appreciable thermal resistance and capacitance, however, we have a condition more nearly analogous to an electrical transmission line with distributed resistance and capacitance than to an electric circuit with concentrated constants. If the insulation is thin, it is possible to get reasonably accurate results by the use of a single exponential term to represent the entire cable, just as it is possible to represent a short transmission line with distributed constants by an equivalent circuit with concentrated constants. When the line gets long, or when the insulation gets thick, due allowance must be made for the fact that the constants are actually distributed. Various people, including the present writer, Mr. Church of Boston, and Mr. Miller of Chicago, have developed methods of doing this, but they all lead to a series of the general form of equation 3 in Mr. Kidder's paper and are quite complicated to apply in practice.

Incidentally, it should be noted that while successive terms of this series may converge quite rapidly toward zero the rate of change in each term does not converge nearly so rapidly. The use of distributed constants is therefore more important in problems involving the rate of change in temperature than in problems involving only the temperature itself. Experience indicates that these additional terms contribute materially to the rate of change in temperature for a period ranging from 5 minutes to 30 minutes after the application or dropping of load; this may affect the choice of the first point on the curve when analyzing time-temperature characteristics empirically in accordance with the method outlined in appendix *A* of Mr. Kidder's paper.

Mr. Kidder's second exponential term represents the cooling of the duct bank rather than that of the cable itself. Empirical information of this sort is extremely valuable, since it is very difficult to establish accurate mathematical equations for the cooling of a duct bank, and even if these equations were established, they would be of little value without the corresponding constants which can only be obtained empirically

One point on page 9 of Mr. Kidder's paper deserves a little further consideration. The writer is of the opinion that the threshold temperature T_0 should correspond to the sum of the ambient temperature, a steady-state rise caused by no-load losses, and the copper-temperature rise corresponding to the actual cable temperature at the time of the application of the overload rather than to the average daily copper loss. For the sake of safety, it would probably be advisable to assume that the overload occurs at or

near the peak of the daily load cycle, since there is absolutely no guarantee that it shall not occur there, and since if the overload persists for as long as 24 hours it is absolutely certain to hit the peak of the daily load cycle at some time or other. Just how close it should be assumed to the peak is a matter for individual judgment; but certainly it would appear that it should be assumed to hit the cable at some temperature higher than that caused by the average daily losses.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): The author's approach to the problem of emergency ratings is interesting and should help in working out individual situations. However, to deduce generalities by means of probability laws requires many basic assumptions, some of which are not as yet well established. A more definite idea on the matter will come from more experience than just the three years of operation of 22 miles of cable at high temperatures cited by the author.

As pointed out in my paper ("Load Ratings of Cable," AIEE TRANSACTIONS, volume 58, 1939, pages 535-56) cable movement is not directly proportional to the square of the load, as was assumed by the author. Also, the rate of sheath troubles due to repeated daily bending in the manhole increases more rapidly than the first power of the magnitude of the cable movement. Furthermore, consideration of troubles should not only include the very important matter of sheath cracks but should also include the effect of high temperature on the insulation. This is especially important for some of the old high-voltage cables which have high dielectric losses. In some of the newer cables with thick insulation and oil fed at the joints, the effects of oil movement and sheath expansion must also be considered.

Any problem of loading including emergency loading must consider the economics for the system as a whole and the probable shortening of the life of the cable by the use of the higher temperatures.

A. H. Kidder: As should perhaps be expected for a paper of this sort, the discussion has been the more helpful because space did not permit a complete summary of the thoughts behind each of its observations. The points raised will be reviewed in the order in which they were brought out in the discussion.

V. M. Montsinger's comments on the author's suggestion for determining the approximate hot-spot temperature in a test on an oil-filled transformer, give a valuable indication of the probable magnitude of error. One to eight degrees centigrade at 150 per cent load is usually well within the over-all limits of error faced in making the practical compromises between accuracy and convenience that assume important proportions in most electric distribution problems. The author's method assumes that the vertical gradient in the winding is that of the oil alone. The method will, therefore, be in error to the extent that current density and eddy currents distort the vertical temperature gradient. The author's only claim for his method is that it provides a convenient means for approximate recognition, during a test at constant load, of the oil temperature gradient as an important component of the instantaneous hot-spot rise above average winding temperature.

E. R. Thomas's suggestion of the need for considering second contingencies should not be overlooked in dealing with large numbers of units n in equations 1 and 2. Their probable frequency of occurrence and their effect upon the curves in figure 3 of the paper may be evaluated approximately by the method outlined for first contingencies.

The severity of second-contingency loading naturally decreases to some extent when the number of units increases in a given design combination, while the likelihood of facing a second contingency increases with the number of units in the group.

F. H. Buller's thoughts on the possible causes of cable failures at high temperatures are quite similar to those which have occurred to the author from time to time. The instance of daily repeated high-temperature operation for 13.2-kv cables probably stands by itself in that it is much more severe than would be justified in any ordinary situation. Should the cable have been destroyed already, the station economies have been adequate to finance their replacement with new and larger cable. In the meantime, the experience being accumulated from such a full scale "accelerated life test" should in time answer approximately some of the interesting questions Mr. Buller has raised. The point that the author apparently failed to drive home, however, is that the cables cited in this instance have already done on at least 600 occasions what they would not be expected to do once in 100 years, if they were in the service represented by figure 3 of the paper which assumes strict observance of the AIEE limitations for all regular daily operations.

Mr. Buller's suggestion of the need for care in selecting the first point on the curve, when analyzing temperature-time characteristics empirically in accordance with the method of appendix A, raises an interesting point. Practical tabular calculations of the temperature transients which accompany normal or emergency load cycles do not require close agreement between the approximate and the true responses to load impulses of less than one-half hour duration on any assembly, because any possible effect of the circuit transient phenomena which are responsible for inaccuracies in this region will disappear within so very short a time in comparison to the duration of the load cycle under consideration. Emergencies which expire within one hour are almost always easiest and best treated as if the accompanying change in load losses were a rectangular impulse, for which the correct value of the response is directly proportional to the readings available on the true temperature-time characteristic.

The threshold temperature T_0 to which Mr. Buller refers was taken to be that level above or below which all temperature changes move because of deviations p from average copper losses in the cable in question. For safety the author prefers to start the tabular calculation of cable temperature at the time during the day when the normal values of p are changing from negative to positive quantities and to assume arbitrarily that the initial value of Θ_1 is zero, instead of the slightly negative value it would have at that time. To determine the approximate copper temperature at any other time it is then necessary only to use in the tabular calculation, the successive values of p which represent the anticipated time sequence of the instantaneous normal and emergency loads. In this way the calculation automatically will include proper recognition of the temperature attained by the time the emergency load is applied, and without need for redefining threshold temperature.

The maximum temperature is usually reached well toward the close of the day's normal heavy-load period and often within an hour or two after the peak. The highest emergency temperature, therefore, accompanies the state of emergency which begins at such a time that normal loads cannot be restored until just after the close of the peak load period. Often the time required for restoration of normal conditions is short enough so the emergency values of p need not be introduced in the tabular calculations prior to the time when Θ_1 may safely be assumed to be zero, as described above. The calculation may also begin at the same point on the load curve when it is desired to initiate emergency loading still earlier in the day, but should follow the normal values of p in reverse time sequence until the instant the emergency values of p will be applied and then proceed as usual. There is always the alternative of calculating one complete temperature curve for a normal day's load cycle, as a means for determining the time of maximum temperature and the conditions at the time it is then decided to start the emergency temperature calculations.

R. J. Woodrow's discussion contributes material which should be very helpful to those investigating the thermal characteristics of a number of different types of electrical equipment.

The data assembled by Mr. Halperin unmistakably support his comments on cable movement and its effect upon formation of sheath cracks. Each other possible cause of cable failure probably likewise has its own special characteristic, so that the over-all effect cannot be calculated deductively without first establishing all of these relationships. There is much to be gained from actively accumulating the facts needed to evaluate these relationships. In the meantime, however, cases of high-temperature operation such as cited by the author should give a fair, though empirical, indication of the approximate manner in which the over-all failure rate will follow moderate changes in the average-day loading of similar cables. Whether three years, or about 600 load cycles, of high-temperature experience can give a practical answer to the effect of temperatures upon the over-all failure rate, would appear to depend appreciably upon how many such high-temperature load cycles will be imposed upon the cable in question. It is the author's opinion that the AIEE limits are none too conservative for normal operating conditions, also that the possibility of infrequent temperature excesses during emergency load periods should give no cause for alarm.

Transactions

Papers and Discussions Comprising Pages 611–74 of the 1939 Volume

Economical Loading of High-Voltage Cables Installed in Underground Subway Systems

E. R. THOMAS
MEMBER AIEE

AN underground high-voltage cable system is made up of two major components: the cable with its joints and accessories and the underground duct and manhole structure. The question of what size of conductor should be chosen for a particular transmission feeder or as a general standard for distribution feeders has confronted every planning group. The voltage for the system is frequently limited to one or to relatively few different values already existing as a system standard, deviations from which would result in increased cost to obtain terminal facilities. Frequently some underground facilities already exist and when only a few additional feeders are being considered it might seem logical to make use of them. A study of the annual charges may show, however, in some cases, that it will be more economical to build additional new underground facilities rather than further to congest already existing structures.

Since some of these questions arose in connection with the planning of tie feeders and network distribution feeders in New York City the cables in the voltage classes which were standard on that system were investigated.

Table I. Cable Technical Data

Kilovolts	Number of Conductors	Size (Thousands of Circular Mils)	Insulation (Mils)	Maximum Copper Temperature (Deg C)	Dielectric Loss (Watts per Foot)	Thermal Constants (Deg C per Watt per Foot) H_1	H_2	H_3
13.6	3	350	156 × 78	76.0	0.7	1.044	1.74 to 2.00	0.75
13.6	3	800	203	82.0	0.8	0.50	1.28 to 1.58	0.75
13.6	1	2500	203	82.0	0.3	0.732	1.55 to 1.84	0.75
27.0	3	350	297	74.4	1.3	0.89	1.51 to 1.67	0.75
27.0	3	500	297	74.4	1.3	0.792	1.36 to 1.63	0.75
27.0	1	1500	297	74.4	0.4	1.260	1.81 to 2.05	0.75
45.0	3	500	225	75.0	0.4	0.49	1.25 to 1.86	0.75
132.0	1	600	560	70.0	1.2	2.02	1.70 to 1.92	0.75

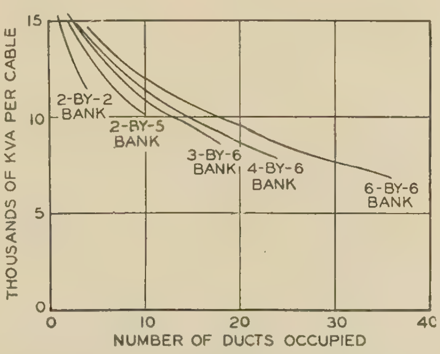

Figure 1. Ratings of cable in ducts

Three conductors, 800,000 circular mils, 13.6 kv

Procedure

The ratings of cables installed in ducts[1] for a given load-cycle and ambient temperature will vary with the number of cables installed in the duct bank and with the size of the duct bank. A typical set of curves for one size of cable is shown in figure 1. It will be readily recognized that for any duct bank the load per cable which may be carried will become a decreasing function as more cables are installed in the bank. This causes the cost per unit of load for cables alone to increase as more cables are added due to the decrease in their rating. At the same time the proportional cost per cable for the duct bank is decreasing due to greater occupancy. The most economical combination will be determined by the ratio of costs between these two component

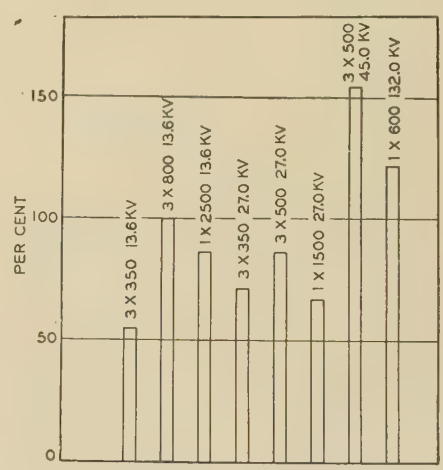

Figure 2. Relative installed cost of cable per cable foot

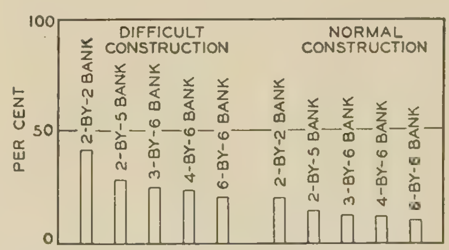

Figure 3. Relative cost of subway per duct foot

Ordinate scale same as figure 2

Paper number 39-69, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 25, 1938; made available for preprinting December 30, 1938.

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The author greatefully acknowledges the help furnished by the various members of the electrical-engineering department in the preparation of this paper. He is particularly indebted to J. M. Comly in the supervision of the assembly of the data and to J. P. Neubauer and J. W. McCloskey for making the detailed calculations and numerous curves.

1. For all numbered references, see list at end of paper.

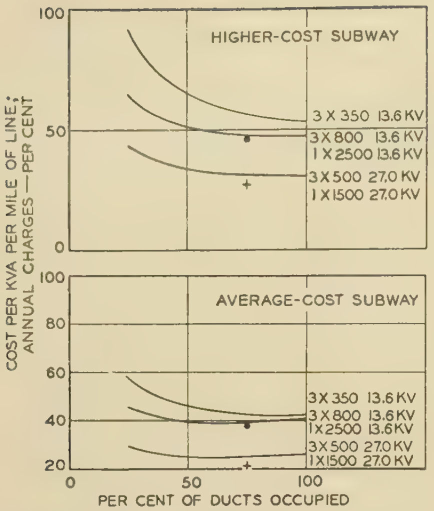


Figure 4. Comparative transmission cost of different cables installed in two-wide by two-high duct bank

parts and the respective changes in cost with increment number of cables.

The sizes of cables which have been selected in this study are typical of those found in practice and a number of them represent the largest size for that type and voltage which are suitable for installation in ducts four inches in diameter. Physical and electrical characteristics of

Table II. Duct Bank Technical Data

Duct Bank: Ducts Wide	Duct Bank: Ducts High	Thermal Constants $H_4 + H_5$ (Deg C per Watt per Foot)
2	2	1.500
2	5	0.857
3	6	0.667 to 0.702*
4	6	0.600 to 0.653*
6	6	0.500 to 0.571*

* Weighted average duct constant used when inner ducts were occupied.

these cables are given in table I. The relative installed costs per unit length of cable are shown in figure 2. These are based on common metal-market prices of copper $12^5/_8$ cents per pound, lead 4 cents per pound.

Duct-bank and manhole costs vary over a considerable range due to the character of the soil affecting excavation and to congestion with other subsurface structures. Two unit costs for these were used in this study, one a higher-cost subway system where excavations were difficult due to soil conditions and congestions of subsurface structures, the other an average-cost subway system where excavation conditions were normal. The relative values of these are shown in figure 3.

The annual carrying charges have been

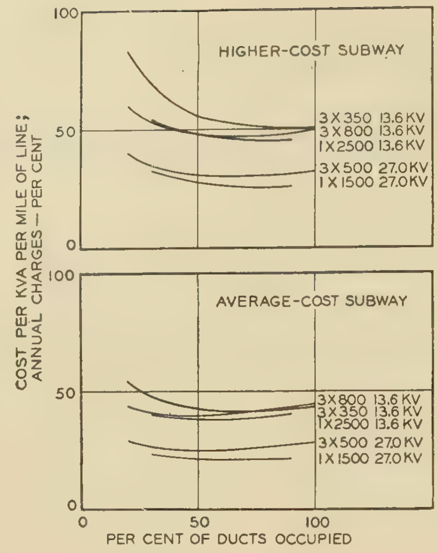


Figure 5. Comparative transmission cost of different cables installed in two-wide by five-high duct bank

Ordinate scale same as figure 4

calculated on the basis of 14 per cent of the installed cable cost and 12 per cent of the subway cost. These values assume an average life of 25 years for the cable and 35 years for the subway system and include interest, insurance, taxes, depreciation, operation, and maintenance. The evaluation of losses was not included since the trend changes due to cost of wasted energy are small compared to the effect of losses in limiting cable ratings.

The ratings of cables used in this study have been calculated for normal allowable copper temperatures, soil ambient

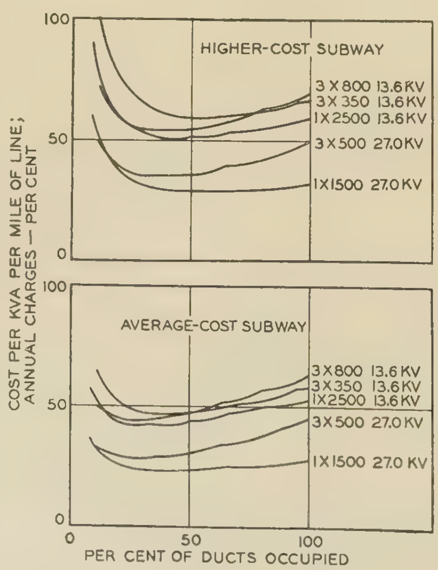


Figure 7. Comparative transmission cost of different cables installed in six-wide by six-high duct bank

Ordinate scale same as figure 4

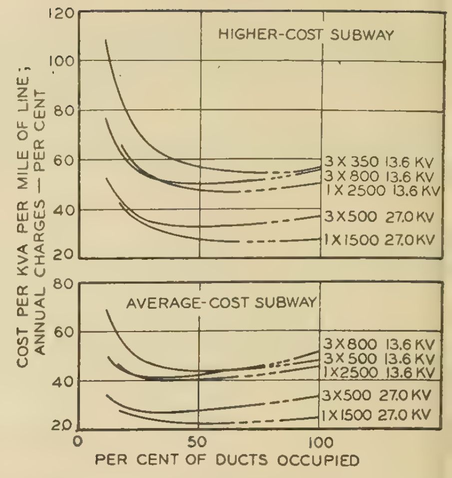


Figure 6. Comparative transmission cost of different cables installed in three-wide by six-high duct bank

Ordinate scale same as figure 4

of 15 degrees centigrade, cable and duct thermal constants as given in tables I and II, 40 per cent loss factor, and 85 per cent attainment factor. The loss factor is the ratio of average to peak loss in the duct bank. The attainment factor is used because the rise in temperature of the copper above the idle duct may be less when the peak load occurs periodically instead of continuously. The ratio of this actual rise to the ultimate for a continuous load is called the attainment factor. These values of loss factor and attainment factor were selected as being representative of the usual loading of duct banks and cables encountered in an underground distribution system. All

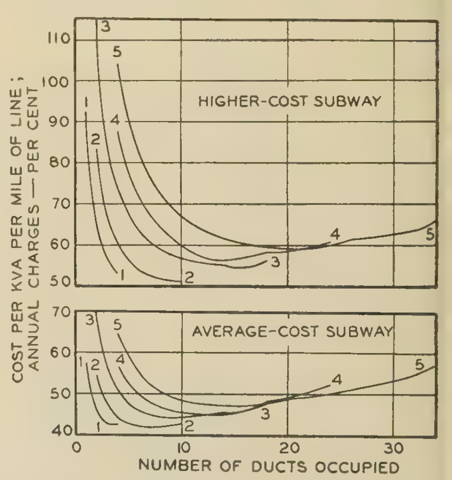


Figure 8. Comparative transmission cost, three-conductor 350,000-circular-mil 13.6-kv cable installed in different-size duct banks

Ordinate scale same as figure 4
Curve 1—Two-by-two bank
Curve 2—Two-by-five bank
Curve 3—Three-by-six bank
Curve 4—Four-by-six bank
Curve 5—Six-by-six bank

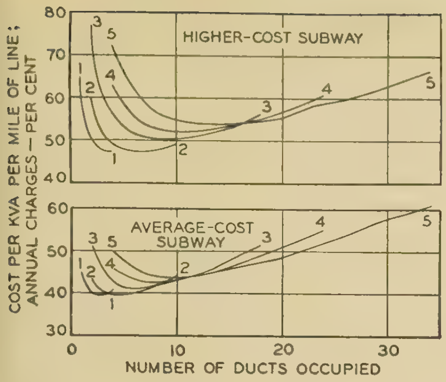


Figure 9. Comparative transmission cost, three-conductor 800,000-circular-mil 13.6-kv cable installed in different-size duct banks

Ordinate scale same as figure 4

For curve designations, see subcaption of figure 8

of the ratings assume three-phase 60-cycle operation. There may be special conditions of operation where a higher loss factor may be encountered. Since the loss factor of all the cables in a bank principally affects the temperature rise of the bank, these special conditions may indicate a somewhat lower rate of occupancy of the bank than with the load factor studied.

Results of Calculations

The annual charges for several types and voltage ratings of cable installed in common-size duct banks are shown in figures 4, 5, 6, and 7, showing relative values both for the higher subway costs and for average subway costs. These data show in general that the largest-size cable for a given voltage class which it is practical to install in a duct results in the most economical high-voltage trans-

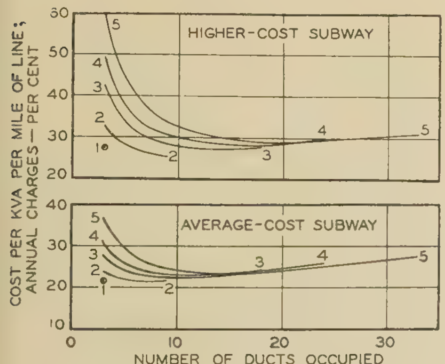


Figure 12. Comparative transmission cost, single-conductor 1,500,000-circular-mil 27-kv cable installed in different-size duct banks

Ordinate scale same as figure 4

For curve designations, see subcaption of figure 8

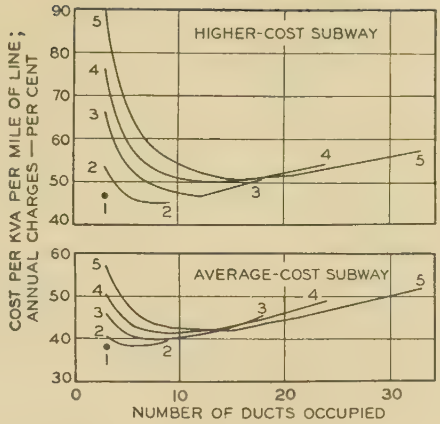


Figure 10. Comparative transmission cost. single-conductor 2,500,000-circular-mil 13.6-kv cable installed in different-size duct banks

Ordinate scale same as figure 4

For curve designations, see subcaption of figure 8

mission. Large single conductor cables are somewhat more economical than three-conductor cable but no account has been taken of the greater voltage regulation which obtains with single-conductor cable and which frequently may make it unsuitable for parallel operation with three-conductor cable.

When an underground high-voltage cable system is made up of one standard size of cable it becomes of interest to know when it is more economical to build new underground facilities to take care of additional cables as compared to installing them in existing facilities. The relative annual charges for several different-size duct banks are shown in figures 8, 9,

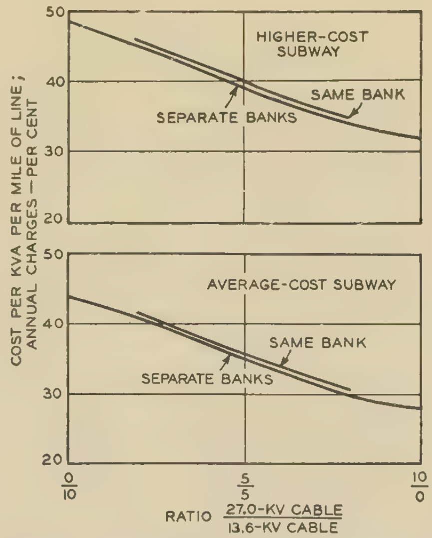


Figure 13. Comparison of transmission cost, 27-kv and 13.6-kv cables co-occupying and separately occupying two-wide by five-high duct bank

Ordinate scale same as figure 4

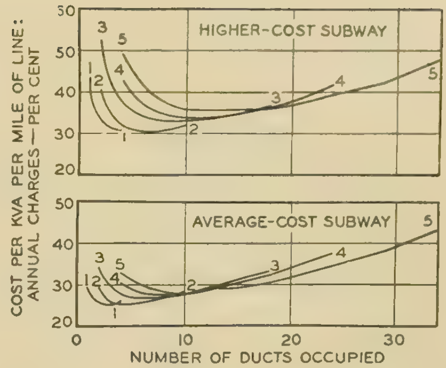


Figure 11. Comparative transmission cost, three-conductor 500,000-circular-mil 27-kv cable installed in different-size duct banks

Ordinate scale same as figure 4

For curve designations, see subcaption of figure 8

10, 11, and 12. As an illustration, it will be noted in figure 9 that if six three-conductor 800,000-circular-mil 13.6-kv cables are already installed in a two-by-five duct bank of average-cost subway and there is need for adding two, three,

Table III. Effect of Load Division Between Cables in a Two-by-Five Duct Bank Containing Five Three-Conductor 500,000-Circular-Mil 27-Kv Cables and Five Three-Conductor 800,000-Circular-Mil 13.6-Kv Cables

Load (Kilovolt-Amperes)			Copper Temperature (Deg C)	
27-Kv Cables	13.6-Kv Cables	Total	27-Kv Cables	13.6-Kv Cables
90,000	0	90,000	74.4	
74,000	47,000	121,000	74.4	72.2
72,000	50,000	122,000	74.4	75.8
70,000	52,500	122,500	74.4	79.0
68,000	55,000	123,000*	74.4	82.0
65,000	55,500	120,500	72.9	82.0
60.000	56,500	116,500	68.9	82.0
55,000	57,500	112,500	65.2	82.0
0	63,000	63,000		82.0

* Maximum kilovolt-amperes is obtained when all cables are at their normal copper temperature.

or four additional cables that it is more economical to build a new two-by-two duct bank to take care of these additional cables. However, if the duct bank were of higher-cost subway it would be more economical to continue to utilize the existing facilities in the two-by-five duct bank for all except the addition of four cables. It will be noted from the various data for duct banks in these figures that two-wide bank construction results in lower over-all annual charges on underground high-voltage cable systems than any of the wider bank widths. This is a somewhat fortunate condition as racking facilities in manholes are greatly improved

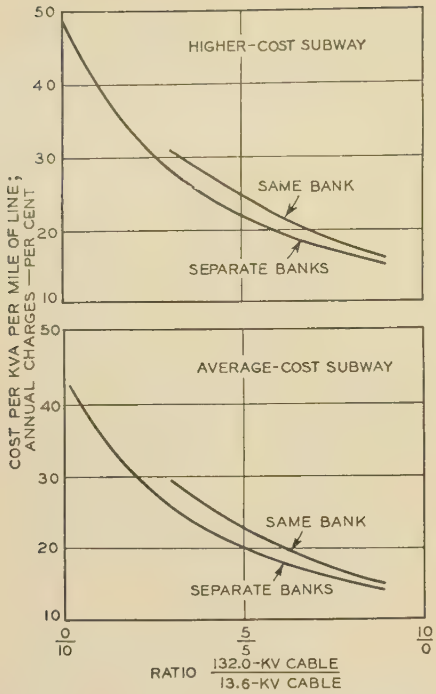


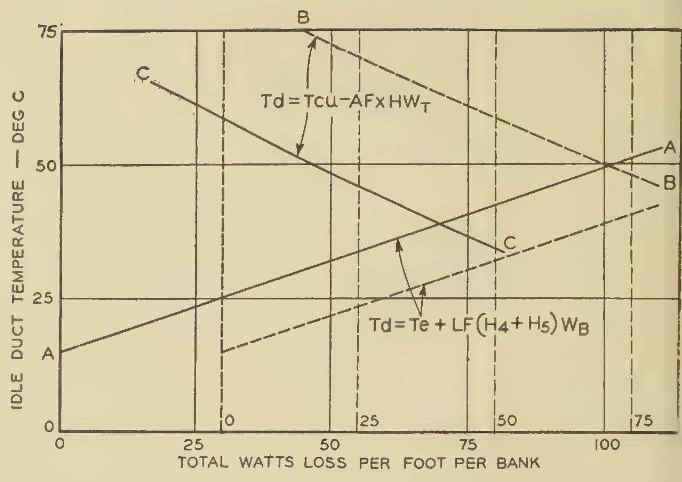
Figure 14. Comparison of transmission cost, 132-kv and 13.6-kv cables co-occupying and separately occupying two-wide by five-high duct bank

Ordinate scale same as figure 4

Figure 15. Graphical solution of ratings of cables co-occupying a two-wide by five-high duct bank

Solid line—Five three-conductor 500,000-circular-mil 27-kv cables

Dashed line—Five three-conductor 800,000-circular-mil 13.6-kv cables



when there is no crossing of cables over duct positions as frequently occurs when duct-bank widths of greater than two are used.

Usually more than one high-voltage class of cable exists on a system and it has been common practice to install cables operating on these different voltages in the same underground duct facilities. These cables have different maximum allowable copper temperatures and different thermal properties. Different voltage classes of cable which are not widely different in their thermal properties and which are installed in two-wide duct banks usually will transmit the maximum kilovolt-amperes through the bank when the load is so proportioned between the two classes of cable that they both reach their allowable copper temperature at the same time. A typical set of data is shown in table III for variously apportioning load on five three-conductor 800,000-circular-mil 13.6-kv cables and five three-conductor 500,000-circular-mil 27-kv cables installed in a two-wide by five-high duct bank. Similar data are shown in table IV for six single-conductor 600,000-circular-mil 132-kv cables and four three-conductor 800,000-circular-mil 13.6-kv cables. This illustration, which deals with cables of widely different voltage classification, shows the marked reduction in capacity which obtains due to occupancy of the same subway system. In order to study the limiting condition when two different voltage classes of cable are installed in a common duct bank we have found that the graphic solution as described in appendix I is very helpful.

Under a given set of conditions the maximum load which may be transmitted on both types of cable through the duct bank may not necessarily be the most economical manner in which to transmit the load. If we consider a system made up of a sufficiently large number of cables of two voltage classes and compare the relative annual charges of installing those cables as a mixed system in common duct banks as compared to installing each of the two voltage classes of cable in separate duct banks, it will be found that the relative annual charges are less when cables of the same voltage classification only are installed in common duct banks. The comparison between three-conductor 800,000-circular-mil 13.6-kv cables and three-conductor 500,000-circular-mil 27-kv cables is shown in figure 13 and a similar comparison between three-conductor 800,000-circular-mil 13.6-kv cables and single-conductor 600,000-circular-mil 132-kv cables is shown in figure 14. However, when the increment number of cables to be added in an existing system of other-voltage cable is small, the most economical arrangement will have to be studied as a specific case.

Table IV. Effect of Load Division Between Cables in a Two-by-Five Bank Containing Six Single-Conductor 600,000-Circular-Mil 132-Kv Cables and Four Three-Conductor 800,000-Circular-Mil 13.6-Kv Cables

Load (Kilovolt-Amperes)			Copper Temperature (Deg C)	
132-Kv Cables	13.6-Kv Cables	Total	132-Kv Cables	13.6-Kv Cables
279,000	0	279,000*	70.0	
222,000	43,000	265,000	70.0	72.0
217,000	45,000	262,000	70.0	77.6
211,000	48,000	259,000	70.0	82.0
192,000	48,300	240,300	66.0	82.0
168,000	49,000	217,000	62.0	82.0

* Maximum kilovolt-amperes is obtained when there is no load on the 13.6-kv cables.

Conclusion

1. Increased operating economies will obtain by properly co-ordinating the selection and loading of cables in specific duct banks.

2. The use of duct banks having more than two-wide arrangements in general are not economical.

3. The construction of new underground facilities frequently may result in a more economical system as a cable system is being expanded, than would an increase in congestion of existing facilities.

4. In general, it is not economical to install cables of widely different voltage classification in a common duct bank.

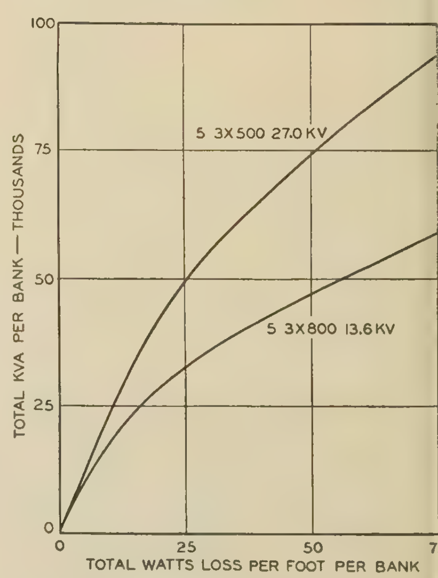


Figure 16. Heat loss in two-wide by five-high duct bank

Appendix I. Graphical Method of Determining the Effects of Load Division on Kilovolt-Amperes for a Duct Bank Containing Cables of Two Voltage Classes

The effects of load division on kilovolt-amperes for a duct bank containing cables of two voltage classes may be determined graphically. This method requires curves of idle duct temperature versus watts loss, the curves for different cables being plotted on separate sheets, and curves of kilovolt-amperes versus watts for each cable.

By superimposing one set of curves on another, the watts loss for each cable at rated copper temperature, or the watts loss on one cable if the loss on the other is known, may be determined. The watts may be converted to kilovolt-amperes by a kilovolt-amperes-versus-watts curve. It is possible also to determine the copper temperature of the cables that are operating below their rated copper temperature.

Two idle-duct temperature curves are needed for each cable, one calculated by subtracting the thermal drop from copper to idle duct from the rated copper temperature (equation 1) and one calculated by adding the thermal drop from idle duct to base earth to the earth ambient (equation 2).

$$T_d = T_{cu} - AF \times (H_1 + H_2 + H_3) W_t \quad (1)$$

T_d = idle duct temperature in degrees centigrade

T_{cu} = copper temperature in degrees centigrade

H_1 = thermal constant for insulation (Simmons)

$= \dfrac{0.00522\ RG_1}{N}$

H_2 = thermal constant, sheath to duct wall

$= \dfrac{4.9}{D(1 + 0.013W_t)}$

H_3 = thermal constant, duct wall to idle duct, 0.75 (Kirke). H_1, H_2, and H_3 are in degrees centigrade per watt per duct foot

AF = attainment factor for rise of copper above idle duct

R = thermal resistivity of insulation in degrees centigrade per watt per centimeter cube

G_1 = geometric factor (Simmons)

W_t = total watts loss per duct per duct foot

D = outside diameter of cable—inches

N = number of conductors

$$T_d = T_e + LF \times (H_4 + H_5) W_t \quad (2)$$

T_d = duct temperature in degrees centigrade

LF = loss factor for duct bank

$H_4 + H_5$ = thermal constant for duct bank in degrees centigrade per watt per duct foot (Kirke)

W_t = total watts loss per duct foot for the duct bank

T_e = earth temperature in degrees centigrade

In equation 1 values for the total watts loss per foot of cable are assumed, and the idle duct temperature is calculated. If there are five cables of one type in the bank, this idle-duct temperature is plotted against five times the assumed watts per cable. Values of total watts per foot per bank are assumed for equation 2.

The application of the graphical method is shown in the following example. Assuming a two-by-five bank containing five three-conductor 500,000-circular-mil 27-kv cables having a total loss of 30 watts per foot and five three-conductor 800,000-circular-mil 13.6-kv cables, the problem is to find the kilovolt-amperes which can be transmitted through the bank and the copper temperature of the 27-kv cables if the 13.6-kv cables are at rated copper temperature.

Idle-duct temperature curves are drawn for each cable and the 13.6-kv curves are placed on the 27-kv curves so that the zero-watt 13.6-kv ordinate coincides with the 30-watt 27-kv ordinate. This is shown in figure 15 where the curves for each cable are drawn on one sheet to simplify the explanation in preference to using separate sheets. The 13.6-kv cables will be at rated copper temperature where the cable and bank duct temperature curves *AA* and *BB* intersect. The watts loss for the five 13.6-kv cables is found by projecting this point to the 13.6-kv watt scale to be 71 watts. Using the kilovolt-amperes-watts curves (figure 16) for the five 27-kv and five 13.6-kv cables the kilovolt-amperes per each set of cables is found and added to get the bank kilovolt-amperes. By repeating this process for various assumed watts loss on each set of cables the correct load division for maximum kilovolt-amperes may be found.

The kilovolt-amperes-watts curves are calculated using resistance and dielectric loss values at rated copper temperature. This will introduce some error when one type of cable is operating below the rated copper temperature.

The copper temperature of the 27-kv cables in the example given is found in the following manner: The idle-duct temperature of the bank is indicated by the intersection of *AA* and *BB* to be 49.5 degrees centigrade. However, at 30 watts per foot on the five 27-kv cables the thermal drop from rated copper to idle duct indicates a required duct temperature of 58.5 degrees centigrade as shown by the intersection of *CC* and the zero-watt 13.6-kv ordinate. The difference between the actual duct temperature and this required duct temperature, 9.0 degrees centigrade, subtracted from the 27-kv rated copper temperature, 74.4 degrees centigrade, gives a copper temperature of 65.4 degrees centigrade for the 27-kv cables.

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Discussion

L. I. Komives (nonmember; The Detroit Edison Company, Detroit, Mich.): The data presented in table IV, concerning the effect of load division between cables of widely different voltage ranges, certainly merits considerable interest. It is, of course, understood that the cables described were compared on the basis of normal temperature limits. However, as oil-filled cables can be operated at higher temperatures than solid-type cables with the same factor of safety, this table should be used only when all other factors are taken into consideration.

J. M. Comly (Consolidated Edison Company of New York, Inc., New York): In Mr. Thomas' paper, figures 8, 9, 10, 11, and 12 show curves of relative annual cost per kilovolt-ampere for various combinations of cables and ducts. It may be of interest to mention an investigation of the effect on these curves of changes in copper temperature and loss factor.

The normal kilovolt-amperes used in calculating cost per kilovolt-ampere might be reduced in practice by the necessity of carrying contingency loads on some of the cables without exceeding their normal allowable copper temperature. Thus a reduction in copper temperature for normal operation would be necessary. Similarly, loads of different types would result in different daily loss factors. Obviously if changes in these constants materially affect the relative costs for different cable and duct combinations, the curves presented in the paper will not show the relative economy of these different combinations except for very special cases.

A study of the effect of changing copper temperature for the special case of three-conductor 800,000-circular-mil 13.6-kv cable indicates that changes in copper temperature from the normal allowable value of 82 degrees centigrade to 50 degrees centigrade and even to 30 degrees centigrade make no appreciable change either in the point of lowest cost nor in the relative costs in different-size duct banks.

Mr. Thomas' data are based on an assumed daily loss factor of 40 per cent. It seems unlikely that the load of large-capacity distribution feeders would vary its character more than would be indicated by a variation in loss factor from 30 per cent to 50 per cent. In this range, the changes in location of the point of minimum cost are negligible for three-conductor 800,000-circular-mil 13.6-kv cable operating in a two-by-five duct bank. If the range is from 20 per cent to 100 per cent, the point of minimum cost in the curves of cost per kilovolt-ampere versus number of ducts loaded may vary as much as two ducts.

In conclusion, it appears from a study of the effects of changes in these two essential constants that the relative costs shown by Mr. Thomas' curves would be reasonably accurate for all practical operating conditions within the range of subway and cable costs shown.

W. F. Davidson (Consolidated Edison Company of New York, Inc., New York): In figure 1 of his paper, Mr. Thomas has used a figure of 1.3 watts per foot for the dielectric loss on 27-kv three-conductor cables. This is probably a representative value but it is by no means a maximum. In the course of some laboratory studies a few years ago, it was found that some cables, when subjected to overload-cycle aging tests, experienced a very large increase in dielectric loss. This suggested the desirability of examining samples removed from service and assumed to be typical of cable installed on the system. A disturbing number showed dielectric losses at rated voltage and 80 degrees centigrade of more than 5 watts per foot with isolated cases going to more than 10 watts per foot. Careful study of the operating records failed to disclose any evidence indicating previous high operating temperatures, nor was there any clue to the cause of the large increase. Some day, we hope to know the answer but until we do, it will be necessary to use some caution in calculating possible maximum temperatures or else be willing to accept the inevitable failure that will occur if a piece of this deteriorated cable happens to come at a point where the duct temperatures are somewhat higher than average.

F. H. Buller (General Electric Company, Schenectady, N. Y.): The writer wishes to endorse Mr. Thomas' conclusions most heartily.

The first conclusion is self-evident.

With regard to the second conclusion, the company with which the writer is associated has always contended that the most economical type of duct construction is a bank two ducts wide and deep enough to accommodate as many cables as may be required. Mr. Thomas has brought out in his paper that the use of duct banks with inside ducts is seldom economical, thus bearing out this contention.

With regard to the third conclusion, the company with which the writer is associated has always contended that the use of more than 12 cables in a duct bank is seldom economical. Mr. Thomas' curves, particularly figure 9, indicate that it is seldom economical to use more than 10 cables in a duct bank, even with a comparatively high-cost subway. While the inclusion of the cost of losses might increase this number of cables somewhat on the basis of annual charges, the writer is in full agreement with Mr. Thomas that a highly congested duct system is liable to be quite uneconomical in the matter of current-carrying capacity.

With regard to the fourth conclusion, this also has been the contention of the company with which the writer is associated, since the sacrifice of load on the higher-voltage cable may quite often exceed the total load carried by the low-voltage cable, and even if it does not, and the total load carried by the duct bank is increased by the installation of the low-voltage cable, nevertheless there may well be a definite sacrifice in economy by following this procedure.

With regard to appendix I, Mr. Thomas has followed the Kirke method of calculating cable ratings. This method is somewhat more complicated than the usual method adopted by the Edison Electric Institute and the Insulated Power Cable Engineers Association in preparing current loading tables, but as used by Mr. Thomas would not give widely differing results.

On the one hand, Mr. Thomas uses an ambient temperature for New York City of 15 degrees centigrade, whereas the usual method would be to base calculations on an ambient of not less than 20 degrees centigrade, on the basis of the curve of earth temperature shown in figure 14 of Mr. Kirke's paper (AIEE Journal, October 1930, page 855). On the other hand, the usual method omits the heating constant H_3 altogether, or rather includes it in the duct constant D. In general, the usual duct-heating coefficient tends to be somewhat lower than Mr. Kirke's values, especially for small numbers of cables in the duct bank, and this offsets the difference in ambient. The resulting differences in current carrying capacity are shown in tables I and II of this discussion.

It will be seen that the usual method gives somewhat higher current-carrying capacity than the Kirke-Thomas method for small numbers of cables in the duct bank, but that the discrepancies involved are not very large.

With regard to the graphical method for evaluating current-carrying capacity when two different types of cables are installed in the same duct bank, the plan which Mr. Thomas outlines in his paper should work very well if the duct bank contains a group of cables which are not fully loaded, and it is desired to find how much load an additional group of cables can carry without exceeding permissible operating temperatures. It appears, however, to be limited to two types of cables, and involves a considerable amount of cut-and-try if it is desired to determine what load each group of cables should carry in order to operate at its maximum permissible copper temperature.

The writer has developed a graphical method which can be used quite readily for any number of different types of cable, and which will give the load which each cable can carry when operating at its maximum permissible copper temperature, or any other arbitrarily selected copper temperature, without resorting to cut-and-try. Since the method is very simple to apply, it might be well to outline it here.

Table I. Cables and Duct Arrangements Covered in Mr. Thomas' Paper

Number of Cables in Duct Bank	*Ratio
4	108
10	101
18	96

* Ratio of $\dfrac{\text{current rating by usual method}}{\text{current rating by Kirke-Thomas method}}$

Table II. Usual Standard Groupings

Number of Cables in Duct Bank	Ratio
1	1.12
3	1.09
6	1.05
9	1.02
12	0.99
*15	0.97
*18	0.96

* These groupings are not standard, but are obtained by extrapolation using the standard method

List of Symbols

T_c = copper temperature, degrees centigrade, equally loaded cables
T_{cn} = copper temperature of cable n (unequally loaded cables) in degrees centigrade
T_0 = earth ambient, degrees centigrade
T_d = duct temperature rise in degrees centigrade
W = loss per cable, watts per foot (for similar equally loaded cables)
W_n = loss in cable n in watts per foot (for unequally loaded cables)
L = loss factor, as a decimal (for equally loaded cables)
L_n = loss factor, as a decimal in cable n (for unequally loaded cables)
N = number of cables in duct bank
H = duct heating constant
D = "duct constant" for equally loaded cables = HLN
R_{in} = thermal resistance of insulation, for cable n
R_{sn} = thermal resistance of surface for cable n
R_i = thermal resistance of insulation (similar equally loaded cables)
R_s = thermal resistance of surface (similar equally loaded cables)

The usual equation for equally loaded cables is:

$$T_c - T_0 = W(R_i + R_s + D) = W(R_i + R_s) + WLNH$$

Or, since $WD = WLNH = T_d$

$$T_c - T_0 = W(R_i + R_s) + T_d \tag{1}$$

If there are N equally loaded cables:

$$NWL = (WL + WL + WL \ldots) \text{ to } N \text{ terms}$$

which may be written

$$\sum_{n=1}^{n=N} nWL$$

Now, suppose that the N cables are unequally loaded but the average value of the product (watts loss times loss factor) is equal to WL. The duct temperature T_d will be the same as before.

Then

$$NWL = \sum_{n=1}^{n=N} nW_nL_n$$

for the unequally loaded case.

So that the duct temperature, T_d, which is the same as before, will be

$$T_d = HNWL = H \sum_{n=1}^{n=N} nW_nL_n \tag{2}$$

Also, from equation 1

$$T_{cn} - T_0 = W_n(R_{in} + R_{sn}) + T_d \tag{3}$$

or

$$W_n = \frac{T_{cn} - T_0 - T_d}{R_{in} + R_{sn}} \tag{4}$$

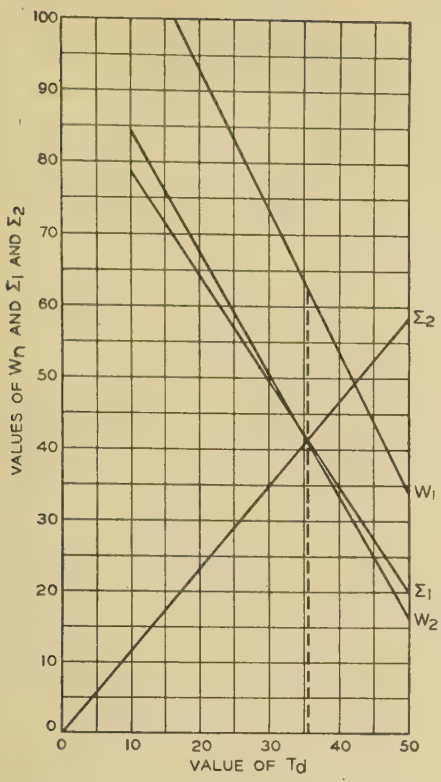

Figure 1. Illustrating graphical method of assigning current ratings to dissimilar unequally loaded cables in the same duct bank

Also, from equation 2

$$\sum_{n=1}^{n=N} nW_nL_n = \frac{T_d}{H} \qquad (5)$$

And, finally, from equations 3 and 2,

$$T_{cn} - T_0 = W_n(R_{in} + R_{sn}) + H\sum_{n=1}^{n=N} nW_nL_n \qquad (6)$$

Assuming arbitrarily chosen values of T_d, plot W_n against T_d from equation 4 for each cable. The quantity W_nL_n should be calculated for each value of W_n, and the summation

$$\sum_{n=1}^{n=N} nW_nL_n$$

should be computed and plotted for each value of T_d. Call this summation $\sum_1$.

Next, compute

$$\sum_{n=1}^{n=N} nW_nL_n$$

from equation 5. Call this quantity $\sum_2$ and plot it against T_d. Where the curves of $\sum_1$ and $\sum_2$ intersect, we have the value of T_d which satisfies both equations 3 and 5.

By dropping a perpendicular from this intersection and noting where it intersects the curves of W_n against T_d, we can find the values of W_n for each cable corresponding to the proper value of T_d, and we can determine the current loading to give these values of W in the usual way.

Since all the curves involved are straight lines, only two points need be calculated on each curve. This makes the arithmetical work very simple, even where several different types of cable are involved.

Example

Two-by-five duct bank—five three-conductor 500,000-circular-mil 27-kv cables, five three-conductor 800,000-circular-mil 13.6-kv cables.

Here we will take $R_{in} + R_{sn}$ as equal to Mr. Thomas' values of $(H_1 + H_2 + H_3) \times 0.85$ and $H = H_4 + H_5$. The usual EEI-IPCEA values could, of course, just as well be used. The loss factor L_n will be taken as 40 per cent or 0.4.

	Cable		
	27 Kv, 500,000 Circular Mils	13.6 Kv, 800,000 Circular Mils	Notes
$R_{in} + R_{sn}$	2.902	2.53	Using lowest value of H_3
T_{cn}	74.4	82	
W_d	1.3	0.8	Dielectric loss
L_n	0.4	0.4	
T_0	0.15	15	
H	0.857	0.857	

Since there are two groups of five similar cables each, it will only be necessary to plot two curves of watts loss, since the cables in each group will presumably be loaded equally, though the two groups will carry different loads.

The values of W_1 and W_2 and also $\sum_1$ and $\sum_2$ are plotted in figure 1 of this discussion. $\sum_1$ and $\sum_2$ intersect at T_d = 35.4 degrees centigrade (duct temperature 50.4 degrees centigrade). W_1 = 62.5 or 12.5 watts per foot of cable. W_2 = 41.5, or 8.3 watts per foot of cable. $W_1 + W_2$ = 104.0. Mr. Thomas' method gives W_1 = 64.5 and W_2 = 44.0, or a total of 108.5 watts per foot of duct bank. The difference is probably due to slide-rule work or graphical discrepancies.

Both this method and Mr. Thomas' method are based on the assumption that the duct heating coefficient is the same for all the cables in the duct bank. This assumption may not hold very closely if some cables are installed in inside ducts. A conservative plan would be to assume a duct heating coefficient for all the cables corresponding to inside ducts; or alternatively, an average duct heating coefficient for the entire duct bank may be used, and the rating for the cables in the inside duct subsequently reduced somewhat, to take care of the higher duct-heating coefficient, which actually obtains in these ducts.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): This paper was interesting and stimulating. However, some of the conclusions apparently do not apply for Chicago conditions.

As a result of detailed cost studies, we have found that the cost of duct banks that are two-wide is higher per outside duct than for a duct bank that is three-wide, prorating the cost of manholes in each case. This applies to a two-by-four duct bank as against a three-by-three duct bank, as well as to a two-by-five duct bank as against a three-by-four duct bank. Generally, the width of excavation in Chicago is about the same for a three-wide conduit as for a two-wide conduit, due to the minimum requirements for working room, while the need for going to extra depth with two-wide conduits results in more labor and interference with other substructures. Another advantage to us in using the three-wide conduits, where there are enough cables involved to justify such conduits, is that the center ducts may be used for signal or relay cables without interfering with the use of the other ducts for power cables.

Our studies of heating constants indicate that a 9-duct conduit has a very slight advantage over the 8-duct conduit, and this applies also for 12-duct conduit as compared to 10-duct conduit, assuming that only the outside ducts are occupied for power cables.

In general, it seems there must be some differences in conditions between New York and Chicago in this matter.

Regarding the author's conclusion 3, we have been endeavoring to limit the size of our conduits to 12 ducts, or to 16 ducts in special cases, for at least the past 12 years.

In general, we have not found it uneconomical to install cables of different voltage classifications in a common conduit. If one were to lay out a brand new system and install all the cables in a given city in a short time, then it certainly would be most economical to install cables of only one voltage classification in a given conduit as far as possible. Our policy is to avoid as much as possible the installation of conduits in a street for five years after it has been repaved. These and other factors affecting system planning plus the fact that the incremental cost of the larger conduits is relatively small mean that many ducts are unoccupied. It therefore becomes necessary to use these ducts as much as possible for new circuits, regardless of the voltage.

It so happens, however, that this practice works out fairly well because it is frequently necessary to limit maximum conduit temperatures to 50 degrees centigrade or less, in order to avoid drying of the soil which would cause conduit and cable temperatures to become excessive. By limiting the normal conduit temperatures to 45 or 50 degrees centigrade, the resulting copper temperatures are reasonable in almost all cases for all types of cable that may happen to be in a given conduit. In connection with emergency loading, only one cable, or three cables in the case of a single-conductor, three-phase line, is subjected to an unusual load and is generating an unusual amount of heat. The emergency lasts just one day, except that it might last two days for an oil-filled line, but during that short time the increase in the temperature of the conduit is only a few degrees. At the same time the cable temperature may safely go to 90 or 100 degrees centigrade or so, that is, to the limit set by the insulation for emergency operation.

E. R. Thomas: It is gratifying to the author that the presentation of this paper should have aroused the interest indicated by the pertinent comments and discussions which have been presented.

W. F. Davidson calls attention to the

Ignitrons for the Transportation Industry

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THE USE of the multianode metal-tank mercury-arc rectifier is well established in the transportation industry. At the present time there are approximately 500,000 kw of these rectifiers in operation on railway properties in America. The units vary in size from 500 to 3,000 kw and range from 500 to 3,000 volts direct current.

The mercury-arc rectifier has replaced rotating conversion equipment for transportation service largely because of its higher efficiency, particularly at low loads, lower installed cost, increased reliability, less maintenance, simpler control, no noise or vibration, ability to carry short-time heavy load swings, and its instant availability for service.

Although, for these reasons, the conventional rectifier amply justifies itself, it has been realized that it does not take full advantage of the possibilities inherent in the mercury-vapor arc. The voltage drop in a simple, high-current mercury arc is less than ten volts and the reverse voltage that such a structure will withstand is many times the value encountered in the transportation field. However, in some as yet not fully understood manner, a simple mercury-arc arrangement has been found to break down occasionally in the reverse direction or arc back, at voltages in the range required. In the conventional multianode rectifier, the anodes are removed from exposure to the cathode and are surrounded by shields and grids in order to provide the necessary reliability. This complication of the structure results in an arc drop varying from 20 to 30 volts, depending upon the tank size, the amount of increase being in proportion to tank size.

Figure 1 shows a cross section of a typical conventional mercury-arc rectifier, and figure 2 is an external view.

The ignitron, as conceived by Slepian and Ludwig,[1] is a major step in the progress toward the ideal mercury-arc rectifier. This type of rectifier is now a practical device as established by excellent operation in commercial service.

In the coal-mining industry, which is largely transportation, there are installed a total of 6,000 kw of mercury-arc rectifiers. Of this total, approximately 50 per cent, or eight units, consists of ignitrons. They vary in size from 300 to 400 kw and operate at 275 and 600 volts direct current. Service ranges upward to two years.

There are now in service or on order, for railway application, two mercury-arc rectifiers of the ignitron type. These units are each 3,000 kw in size, they have the heavy-duty rating, and are in the 600-volt d-c class. These units have an anode rating equal to that of the largest multianode rectifier now built for railway service. One of these units is for service on the subway system of the Board of Transportation of the City of New York, and the other is for service on the main-line electrification of the New York Central Railroad.

Principle of the Ignitron

A cathode spot is the essential element of an arc. With a cathode spot, in a low-pressure gas chamber, any anode will pick up current when a positive potential is applied. Since a cathode spot cannot be created reliably in a low-pressure gas by the application of high voltage, it is necessary to start a rectifier by some other means. In the conventional rectifier, this is done by drawing an arc by separating electrodes at the cathode surface. The cathode spot thus formed is maintained continuously by a small current to an auxiliary anode. This arc current results in ionized gas. It is obvious that economy of equipment and auxiliary power is effected by placing several power anodes in the same tank. This is the reason for the multianode rectifier. The continuous presence of ionized gas, which includes the time that the anodes are bearing reverse voltage, greatly facilitates the formation of a cathode spot on an anode which is the principal reason for the shields and grids as previously mentioned.

The ignitron principle provides a method of starting an arc reliably in a few microseconds. This method is ame-

Paper number 39-39, recommended by the AIEE committee on transportation, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 23, 1938; made available for preprinting January 3, 1939.

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1. For all numbered references, see list at end of paper.

fact that the dielectric loss of cables withdrawn from service has been found to exceed the average values used in calculating the ratings for the paper in many instances. This is a fact which should be considered in any investigation of a specific duct loading problem. Its effect on the results will be to indicate that the greatest economy can be obtained with smaller duct structures when and if such high-loss cables are used.

Mr. Halperin's comment on conditions in Chicago is very interesting. It would seem that where a requirement for duct space for supervisory or signal cables exists, a very real economy might be obtained by the use of the three-wide rather than the two-wide duct structure.

Mr. Buller's general agreement with the conclusions of the paper is appreciated. The variations he mentions between the ratings obtained by the EEI method and the Kirke method are recognized. However, it is felt that a somewhat greater complication of the Kirke method gives greater accuracy than the EEI method does.

Mr. Buller's comment on the graphical method given in the paper suggests that it involves the use of cut and try. By the superposition of several curves drawn on different sheets of transparent material, it is possible to determine accurately the load which each of several cables will carry when all are operating at their maximum permissible copper temperature. With some loss of accuracy, it is possible to use the same curves to determine the load each of several types of cable will carry when operating at temperatures other than the maximum permissible temperatures. The method outlined by Mr. Buller does not appear to be as flexible as the one shown in my paper since one set of curves is good only for one solution of one set of conditions. A simultaneous solution of two equations would accomplish the result with less labor. The W_1 curves shown in figure 1 of Mr. Buller's comments are straight because his R_s is a constant. In my equation H_2 replaces the R_s in Mr. Buller's equation and H_2 varies with the watts lost.

The supplemental data presented by Mr. Comly are interesting since they indicate the probability that the conclusions given in my paper would not be affected materially by normal changes in type of load or average loading.

Mr. Komives' comment on the permissible temperature limit for oil-filled cable seems irrelevant since the figures in table IV are based on the allowable temperature for each of the cables indicated. For the 132-kv cable, the values are those for oil-filled insulation.

In closing, let me re-emphasize the fact brought out by the paper that a considerable saving may be realized by careful analysis of the economies involved in duct-bank size and loading in advance of construction. I am indebted to the discussers for their interest and hope that more analyses of the subject may be made as a result of this discussion.

nable to synchronous application. With such a system of ignition, the arc may be permitted to extinguish completely at the end of each conducting period. This leaves the anode surrounded by deionized gas during the time that it is bearing reverse voltage, except for a few microseconds following the conducting period, which is the transition time required for deionization to take place. Of course, in order to take advantage of this method of operation, each anode with its own cathode is mounted in a separate chamber, thus removing it from the influence of other anodes when they are conducting current. This permits the reduction of the shields and grids to the minimum necessary to take care of the transition period and permits the location of the anode close to the cathode.

The way in which an arc is started by the ignitron principle is described in detail by Slepian.[2] Briefly, when a high-resistance rod is immersed in mercury and a current of sufficient magnitude is passed through the rod to the mercury, the potential gradient set up at the junction between the two materials is sufficient to initiate a cathode spot. The magnitude of current necessary is dependent upon the resistivity of the material used for the rod. It has been found that rods or ignitors of boron carbide or silicon carbide, which are the materials most in use today, have a resistivity such that a current of less than 20 amperes and approximately 100 volts are required. These are convenient values for practical operation. The excitation circuits will be described in a later section.

Construction of the Ignitron

In general, the type of construction used for large power ignitrons is the same as that used for conventional rectifiers. The anode assembly, consisting of a vacuum-tight insulating bushing, anode head, and the current-conducting parts is identical. The tanks are made from specially selected sheet steel with the seams welded vacuum tight. The same type of vacuum-tight gaskets are used for the cover-plate seal and for all separable connections in the vacuum pumping system. The vacuum pumping equipment is identical. The essential differences lie in the separate anode with associated cathode vacuum chamber, the vacuum-pumping manifolding, the simplified anode-shielding structure, and the ignitor with associated control thyratrons of the excitation system.

Figure 3 shows a cross section of an ignitron.

There are a number of features associated with the small vacuum tank that contribute to reliability in service. The vacuum-tight gaskets are smaller. The need for a cathode insulator is eliminated. Copper coils for the water-cooling system are easily applied. The use of copper for external cooling coils and nickel for internal cooling coils eliminates all ferrous materials from the cooling system and reduces the corrosion problem to a minimum.

In order to secure a satisfactory wave form, power rectifiers are usually built with multiples of six anodes. This practice is followed in assembling the ignitrons into units. While, if required, more than six ignitrons can be assembled with a common vacuum-pumping system, greater operating flexibility is permitted, even for large station capacities, when the units are sectionalized.

Figure 4 shows a rectifier unit of six ignitrons.

A great deal of work is now being done on sealed-off rectifiers of both the conventional and ignitron types. So far, sealed-off construction has been confined to relatively small sizes, smaller than the usual transportation requirement. Not enough experience has been obtained to determine the average life of such rectifiers and it is obvious that this factor will

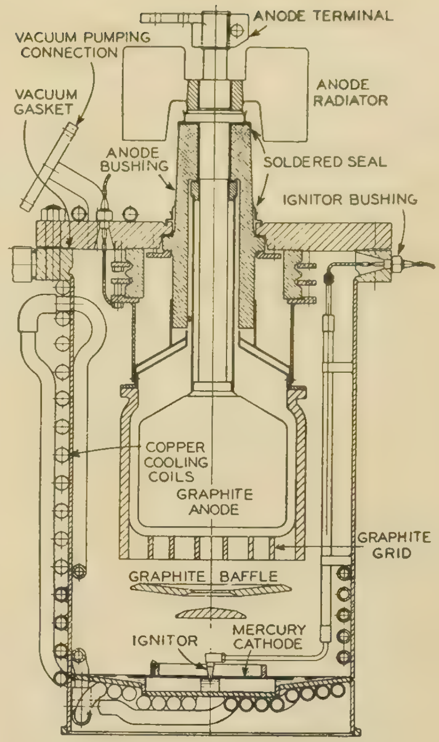


Figure 3. Cross-section view of an ignitron

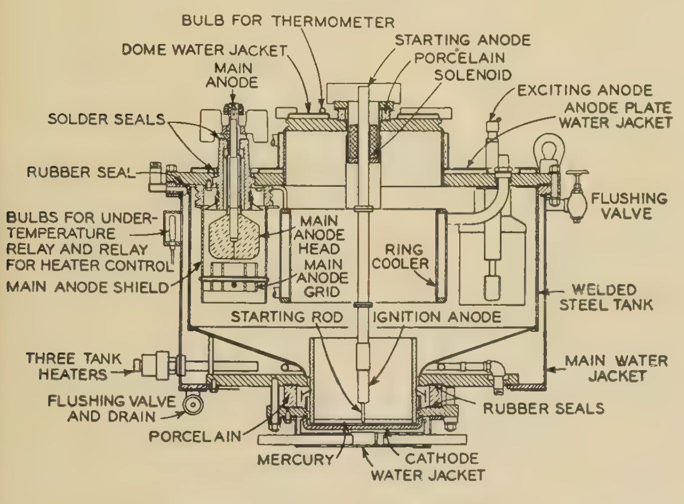


Figure 1. Cross-section view of conventional multi-anode mercury-arc rectifier

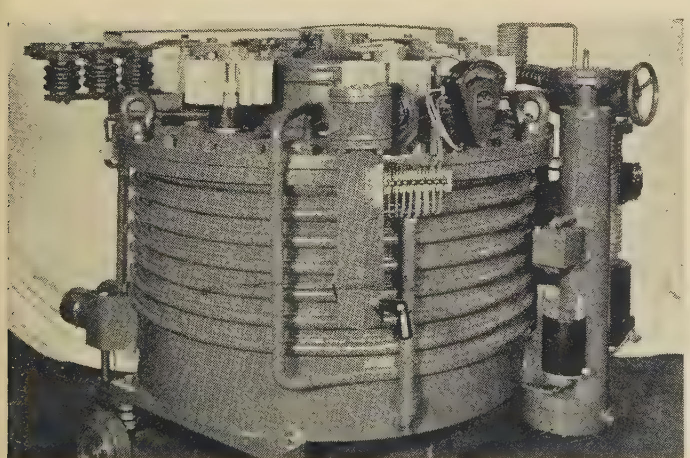


Figure 2. A 750-kw 600-volt six-anode mercury-arc rectifier

determine the maximum size where this type of design is attractive. Since a sealed-off rectifier must be thrown away, or undergo a major factory rebuilding when it deteriorates to a point where service is unsatisfactory, the life must be many years to justify its use in the larger, more expensive sizes. On the other hand, in the small capacity sizes where the cost is low, the cost of periodic replacement is not prohibitive. This must be balanced against maintenance and relatively high initial cost of a vacuum-pumping system. Omission of the vacuum-pumping system is always attractive and an increase in the capacity of sealed-off rectifiers is to be expected.

Efficiency

One of the major advantages of the ignitron is its high efficiency. The factors that affect a rectifier unit efficiency are the losses of the transformer, the losses of the auxiliary apparatus, and the voltage drop in the power arc. The transformer is a highly developed piece of equipment and the losses are established by the economics of design. There is not much margin here on which to work to improve unit efficiency. The auxiliary losses are low and their total elimination would not represent much gain for units of the size used by the transportation industry.

The arc loss of a conventional rectifier constitutes not only two-thirds of the total unit loss but is known to be greatly in excess of that theoretically necessary. The present commercial ignitron, with its arc drop of 14 to 18 volts, is a major advance toward the theoretical minimum from the 20 to 30 volts obtained in the conventional design. In both ignitrons and multianode rectifiers the higher arc drop is associated with the higher ratings.

Figure 5 illustrates the efficiency advantage of the ignitron over conventional rectifiers at 600 volts.

For 275-volt applications, the ignitron has an even more marked advantage in efficiency. In any given type of rectifier the losses are almost proportional to the current and the arc drop is only slightly influenced by the system voltage. Therefore, the arc drop is a greater proportion of the output voltage in the lower voltage classes and an arc drop advantage becomes more important. This is illustrated in figure 6.

Factors Influencing Rating

The major factors which determine the rating of a given design are thermal limit, current instability in the arc, and arc-back frequency.

The thermal limitations of materials used are easily determined and offer no problems from the design standpoint.

Arc-current instability is the cause of voltage surges. In order to transport a given current in an arc, proper ion density must be maintained. The lower the vapor density, which is influenced by the temperature of the cooling surfaces, and the more obstructions in the arc path, the more difficult it is to maintain proper ion density. With an inadequate supply of ions, the arc resistance fluctuates with corresponding current fluctuations, or the arc tends to go out. Sudden decreases of direct current through a reactor, which in this case is the secondary winding of the transformer, cause the stored energy of the reactor to appear as high voltage, or voltage surges. To avoid surges in conventional rectifiers, it has been the practice to maintain cooling-water temperatures above the value at which surges occur. In the ignitron, because of the reduced obstructions in the arc path represented by minimum shields and grids and because of the proximity of the anode to the cathode, where the vapor density is greatest, the tendency to surge is greatly reduced. Incidentally, operating conditions under which surges occur also favor the occurrence of arc-backs. This cause of arc-back is, therefore, materially decreased in ignitron rectifiers.

The most important of the limitations in the design of a rectifier is arc-back. Although the causes of arc-back are not fully understood, they are known to be favored by such things as impurities in materials and foreign dirt particles, poor vacuum, too high mercury-vapor density, surge conditions, and exposure of an anode bearing back voltage to a cathode spot or ionized gas. The first three of these causes are minimized by careful selection of materials, careful shop practice, use of modern vacuum technique, and adequate cooling-medium control. Surge-producing conditions are avoided for normal conditions of operation.

As previously mentioned, the last cause is minimized by the use of shields and grids, and for the conventional rectifier, by the removal of the anodes from the cathode. These shields and grids increase the arc drop and the amount of the increase is proportional to the extent to which the arc-back rate is minimized. Experience with conventional rectifiers in commercial service has established the economic balance between the permissible arc-back rate and efficiency, as influenced by arc drop. Since the ignitron has per-

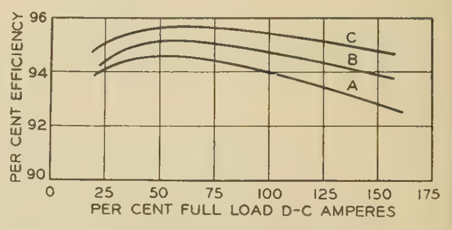


Figure 5. Rectifier unit efficiency curves of 3,000-kw 600-volt voltage-controlled mercury-arc rectifiers with 13,200-volt 60-cycle supply

A—Single-tank 12-anode rectifier
B—Sectional-type 24-anode rectifier
C—Ignitron-type 12-anode rectifier

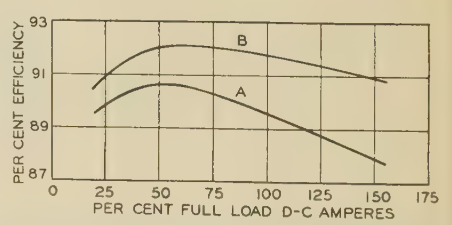


Figure 6. Rectifier unit efficiency curves of 600-kw 275-volt voltage-controlled mercury-arc rectifiers with 2,300-volt 60-cycle supply

A—Single-tank six-anode rectifier
B—Ignitron six-anode rectifier

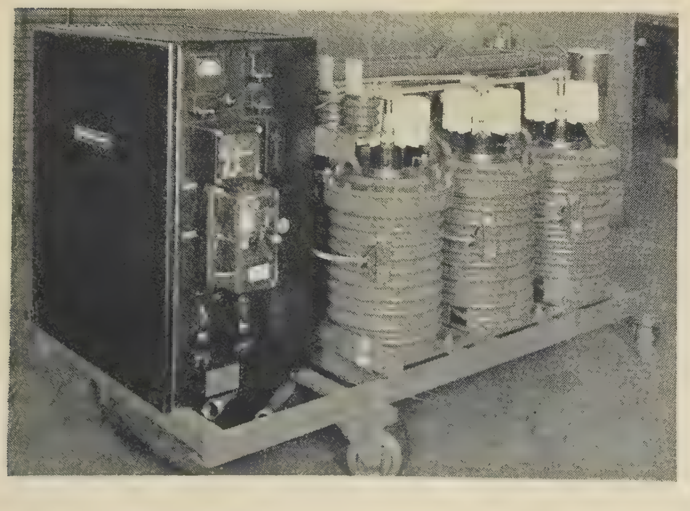


Figure 4. A 1,500-kw 600-volt six-ignitron rectifier unit with vacuum and control auxiliaries

mitted a substantial reduction in shields and grids as well as anode to cathode spacing, by an entirely new principle of operation, there is a new proportionality between arc-back frequency and arc-drop voltage. At present, because there is this new proportionality, in order to provide

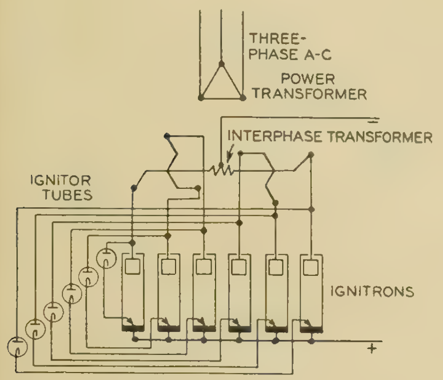


Figure 7. Ignitron rectifier diagram using the anode firing method of excitation without voltage control

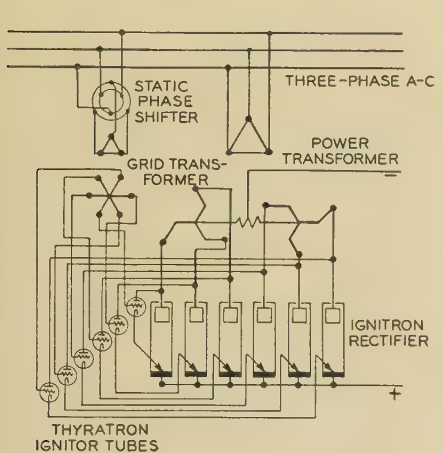


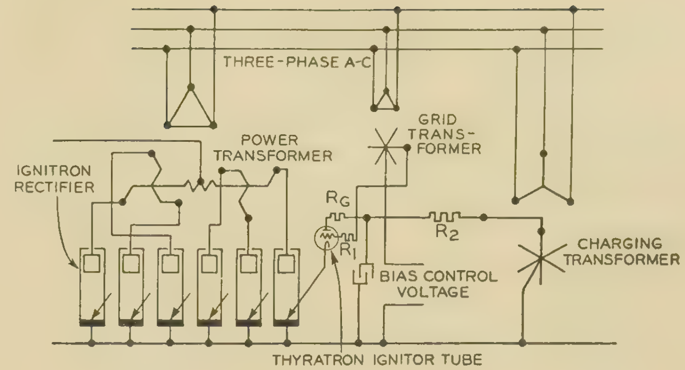
Figure 8. Ignitron rectifier diagram using the anode firing method of excitation with voltage control by phase shift

greater freedom from arc-back, full advantage of the efficiency increase has not been taken.

Special Circuits for Ignitrons

There are several ways by means of which the necessary accurately timed current impulses may be applied to ignitron ignitors. Possibly the simplest is shown in figure 7, which uses anode firing without direct-voltage control. The ignitor power is taken from the main transformer through thermionic-cathode gas-filled ignitor tubes. When the anode of the ignitron and associated ignitor tube become positive with respect to the cathode, current will flow through the ignitor tube because of its thermionic cathode. Upon

Figure 9. Diagram of a capacitor firing method of excitation



the creation of the ignitron cathode spot, caused by this flow of current through the ignitor tube and ignitor, the ignitron will pick up, its arc short-circuiting and therefore extinguishing the ignitor current.

Control of the direct voltage of an ignitron is obtained in the same manner as it is for the conventional rectifier, that is by delaying the pickup of the anodes. However, in the ignitron, this delayed pickup is controlled by the provision of energized grids in the ignitor tubes rather than in the main power arc. This permits voltage control without any sacrifice in efficiency and by the use of relatively very little control energy. Figure 8 shows the devices and circuits involved. Thyratron ignitor tubes, which are thermionic cathode tubes with control grids, are used for the ignitor circuits. The anodes of the ignitron and thyratron tubes become positive with respect to the cathode, as before, but current will not flow through the thyratron tube until its grid is made positive with respect to the cathode by the grid transformer. The grid transformer is energized through a static phase shifter by means of which the phase angle between the potential of the anode and that of the grid may be accurately controlled. In this manner, the formation of the cathode spot in the ignitron is delayed to secure the desired direct-voltage reduction.

Another method of ignitron excitation is shown in figure 9, in which the ignitor current is obtained from a separate source including capacitors and a charging transformer. In the circuit shown, direct-voltage control is obtained by means of a bias voltage in the neutral of the grid transformer rather than by means of a phase shifter.

Other methods of ignitron excitation include transformer without capacitors, peaking transformer with Rectox, rotating impulse generator, and rotating commutator. The last three methods do not require ignitor tubes.

Since the ignitron depends on current flow through its ignitor before an anode can pick up, it is only necessary to block the excitation current to prevent pickup. This blocking can be accomplished either by opening the ignitor circuits, which can be done by relay contacts, or by applying a negative voltage to the grids of the thyratron tubes, if used. Since this involves only the control of low-energy circuits or the blocking of grids of low-energy tubes, "arc snuffing," that is, the interruption of d-c short circuits or of anodes feeding into an arc-back, can be accomplished with great speed and reliability.

Conclusions

The ignitron rectifier brings to the transportation industry a conversion unit having several advantages over the conventional multianode rectifier.

Although, in the interest of high reliability, full advantage is not taken of the possible reduction in arc drop, an efficiency advantage of from one to $1^1/_2$ per cent is realized for 600-volt units and of from 2 to 3 per cent for 275-volt units.

Because control of anode ignition is accomplished through small auxiliary tubes, voltage control is more flexible, has no detrimental influence on efficiency, and requires less control energy. For this same reason, "arc snuffing" can be accomplished with maximum speed and reliability.

Because the single-anode tanks of ignitrons are relatively small, the use of tubing of copper or other corrosion-resisting material for the cooling system is easy. This practically eliminates the corrosion problem.

In performing internal maintenance, only one ignitron need be opened at a time. The smallness and lightness of parts greatly facilitates this operation.

For a given rating, an ignitron unit is lighter and occupies less volume than a conventional rectifier. This results in economy of installation.

The ignitron also has some disadvantages.

There is the necessity for manifolding

separate tanks to a common vacuum-pumping system which complicates the vacuum connections.

The excitation system is somewhat more complicated. Most of the circuits in use today involve the use of thermionic-cathode tubes which require periodic replacement.

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Discussion

D. S. Smith (Northern Electric Company, Montreal, Que., Canada): In the paper on "Ignitrons for the Transportation Industry" the statement is made that as yet the sealed-off construction of rectifiers has been confined to small sizes below the range usually associated with traction substations. It is true that the individual sealed-off unit has not been built in large sizes, say above 1,000 amperes, but by the adoption of the unit principle large rectifier banks have been made possible. Glass is for many reasons an ideal material to use for the envelope of a sealed-off rectifier and the glass bulb rectifier has achieved considerable popularity in some parts of the world.

First of all let me give a rather broad picture by describing briefly some of the large glass-bulb traction installations made by one British company:

A. The Manchester-Bury line of the LMS Railway has two 3,600-kw rectifier substations operating at 1,200 volts direct current. The rectifier transformers are connected 12-phase and no d-c smoothing equipment is used. Each substation has three banks of six bulb units each, with high-speed d-c circuit breakers for each bank. These two substations are two of ten glass-bulb-equipped substations on the LMS totalling 13,200 kw.

B. The Bombay Baroda Railway in India has one 4,000-kw glass-bulb substation comprising two banks of six bulb units each, operating at 1,600 volts direct current. The rectifier, which is connected 12-phase, is provided with d-c smoothing equipment and has automatic grid control to give an overcompounded characteristic.

C. The New Zealand Government Railways have six substations with a total capacity of 6,000 kw operating at 1,600 volts direct current.

D. The British Columbia Electric Railway has two 330-kw substations and one 660-kw substation operating at 550 volts direct current. Their experience with glass bulbs has been the subject of an article in the technical press.

E. The London Passenger Transport Board has a total of 49 glass-bulb substations ranging in size up to 3,000 kw and having a total capacity of 35,500 kw.

F. The largest glass-bulb rectifier substation is one of 7,000 kw in Shoreditch London but this is not a traction job.

G. Finally, to give some idea of the place of the glass-bulb rectifier in Great Britain, I might say that the total installed capacity is over 500,000 kw against 350,000 kw of steel tank—these figures include all power rectifiers, not only those for traction service.

Now let us look at the glass bulb from another angle. In spite of its apparent frailty it can, if it is of approved type, be insured at an annual premium equal to $4^1/_2$ per cent of its value for the nine years following the initial year of service. During the first year it is covered by the manufacturer's guarantee. Mention has already been made of the Shoreditch station and it is interesting to note that no bulb replacements have been made to the three original 2,000-kw banks each with 16 bulbs and installed in 1929, 1930, and 1931, respectively. For a 2,000-kw installation made in 1925 the average annual maintenance cost per bulb unit including bulb replacements has been under $2.25 in spite of the fact that the bulbs are of an old type which could not be repaired. One engineer when asked about maintenance charges on a 3,600-kw traction substation under his charge placed them at about five shillings a week, $1.25, and this included the cost of periodical cleaning up of the substation.

Objections to the use of glass in the envelope of a rectifier are largely psychological and are gradually being overcome as a result of the comparative rarity of breakage in handling. One installation of glass-bulb rectifiers is in a mine in South Africa where the bulbs are subjected to frequent concussions from explosions. The British Admiralty actually carried out tests in which rectifier bulbs were set near large guns during firing and as a result have a glass-bulb rectifier at the Woolwich Arsenal.

Glass-bulb rectifiers are of course characterized by their extreme simplicity, the only auxiliary required being the cooling fan. The bulbs hold their vacuum indefinitely so that no vacuum pumps are required, and control gear is very simple in comparison with corresponding gear for water-cooled steel-tank installations. The strength of the glass bulb really lies in the seals which are simply formed by using a glass and metal combination with two nearly identical coefficients of expansion. The technique of glass manufacture, including of course, the seals, is the foundation for the success of the glass bulb, but it has taken long years to develop the technique and as a result the pioneer manufacture of bulbs in Great Britain still does a very large proportion of the glass-bulb business. There is one other point in connection with the use of glass which must be mentioned and it refers to its transparency. This is an invaluable asset both during processes of manufacture and afterward during operation. Trouble in a glass bulb when it does occur is very readily diagnosed.

Bulbs of the larger sizes are usually of the six-anode type with two or three excitation electrodes continuously energized and starting electrode. The old tilting bulb is a thing of the past and methods of starting are very simple, one method using a flexible electrode which is drawn down into the mercury pool by means of a small external electromagnet. Start-up is practically instantaneous and full load can be thrown on a cold bulb without fear of trouble.

The electrical characteristics of the glass-bulb rectifier are generally similar to those of the steel-tank type. Inherent regulation is usually about six to seven per cent from low load to full load. Efficiencies generally are higher with glass bulbs than for the large tank-type rectifiers as the use of large tanks involves a longer arc path with higher arc drops—the arc drop is of the order of 22 volts in the glass bulb and is thus higher than for the ignitron, but about the same as for small tank-type rectifiers.

Common overload ratings for glass-bulb rectifiers for traction service call for 25 per cent overload for two hours, twice full load for ten minutes, and three times full load momentarily. While higher overload ratings are sometimes called for and can readily be met by a small derating of the bulbs, these are fairly typical figures which have been found satisfactory in practice.

Six-phase connection of glass-bulb rectifiers is most common for traction supplies and smoothing equipment comprises air-cored choke and tuned shunt circuit. Twelve phase is sometimes used, two six-phase bulbs working together, and in such case smoothing equipment has usually been found unnecessary except where grid control was in use.

Backfires or arc backs in glass-bulb rectifiers are of extremely rare occurrence. This is easy to understand when the processes of manufacture by which all impurities are excluded from the bulb are considered. The use of a pure graphite anode reduces the likelihood of hot spots forming on the anode and as the vacuum is permanent there is no trouble from this source. High-rupturing-capacity fuses are normally connected in each anode circuit and these clear any internal faults but high-speed d-c breakers in the output circuit of each bank clear on external faults. Incidentally, all that is necessary to put a bulb back in service after a backfire is to replace the fuses.

Present maximum current ratings of glass bulbs are of the order of 400 amperes at 550 volts and 300 amperes at 1,500 volts, these ratings having been made possible by the efficient use of fan cooling. Experimental bulbs have been built to carry 1,000 amperes at the lower voltages and it would appear that a bulb of this rating would be useful for large installations if there is no sacrifice in efficiency.

Voltage control where required has in many cases been effected with induction regulators or on-load tap-changing transformers. Grid control has been very successfully applied but is not favored very generally on account of the harmonics introduced. Where grid control is used it is considered advisable to apply the debiasing voltage in the form of a steep-front wave, as the alternative method of steadily increasing the amplitude of the positive bias may lead to uncertain timing of the ignition of anodes working in parallel.

Multibulb banks of glass bulbs have the distinct advantage that the loss of one bulb results in only a small decrease in capacity. Further the large number of anodes results in a low current density per anode and this, together with the wide mechanical spacing of the electrode arms helps further to reduce the possibility of backfire.

While it would appear at first that the space required for glass-bulb rectifiers would be large this in fact is not the case. Generally speaking they require little if any more space than equivalent metal-tank types, this being partly due to the smaller amount of control gear necessary with glass.

Finally I would refer to discussions which have been going on with a view to eventually issuing an international specification on mercury-arc rectifiers. While it was originally proposed to have two separate specifications with different test conditions for steel-tank and glass-bulb rectifiers, as a result of the insistence of the glass-bulb manufacturers themselves the two are to be grouped together and the glass-bulb rectifier will thus have to meet the same standards as the steel-tank type.

J. J. Linebaugh (General Electric Company, Schenectady, N. Y.): The authors have given us a good outline of the development of the ignitron type of mercury-arc rectifier for transportation service with a general description of the ignitron principle, as applied to single-anode tanks.

The company with which the writer is connected has been working on the new problems incident to the development of this new type of rectifier for several years with very satisfactory results.

In 1937 the development had reached such a stage that an order was taken for a 3,000-kw 625-volt 12-tank unit for the New York Board of Transportation subway system. This unit has been in service since June 1938 and carries regular loads successfully. It is interesting to note that this unit must deliver 14,400 amperes for one minute. Similar equipment has been sold for service in coal mines.

One of the main problems to be solved in this development is the best method of starting the arc, as regards simplicity and reliability.

A number of different firing schemes have been proposed and several tried with varying degrees of success. Improvements are continually being made for the purpose of simplification and longer ignitor life.

It has been our experience that these 625-volt single-anode tanks can be opened for inspection and then restored to regular service without the necessity of bakeout if each tank is provided with a separate vacuum valve.

Our experience has been similar to that described in the papers and we find the advantages set forth in the conclusion of the paper are amply realized.

This type of rectifier will undoubtedly be a serious competitor of the multiple-anode tank.

S. R. Durand (nonmember; Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The authors of this interesting paper have reviewed the principle of the ignitron tube and compared it with the multianode rectifier tank which today is well established in the transportation industry. When new developments are created which appear to have advantages in comparison to equipment already in use, there is always a period of time in which the value of these new features must be carefully weighed against the ruggedness and reliability of the well-tried and proved equipment. Very often in spite of outstanding new features incorporated in a device, some of these features in themselves may have inherent limitations which will impair the ultimate development of the equipment to the same degree of service reliability as attained in the older equipment. As the development of ignitrons and similar electronic devices proceeds, it is possible that in the near future equipment will be perfected which will most nearly meet in all respects the qualities desired in conversion apparatus for the transportation and other industries.

The authors have described the method of control utilizing the principle of timing the ignition. However, they also mention that use is made of shields and grids in ignitron tanks, and a grid and grid-inlet bushing connector are shown in the cross-sectional diagram of figure 3. It would be interesting to know if grid control is employed in some manner with ignitor control, or if the grid is simply energized to assist in the pick-up of the arc.

The use of external copper and internal nickel cooling coils is preferable to the use of steel water jackets on small tanks even though the problem of corrosion has been materially reduced in most large rectifier installations by means of recirculating cooling systems with heat-exchange units. In some localities the cost of cooling water is an important item in the operation of rectifiers in the transportation industry, so that it would undoubtedly be of interest to many engineers to know if the cooling-water consumption of a group of ignitrons can be reduced in comparison to multianode tanks under the same load conditions.

O. K. Marti (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The authors present in a very interesting way the principle of the so-called "ignitrons" consisting of a tube or tank with a single anode having a cathode and an ignition device called the "ignitor," from which this kind of rectifier took its name. A very instructive comparison is made between the design as well as the characteristics of this rectifier with a conventional multianode rectifier which today is well established in the transportation field.

The main feature of operation of this rectifier is to establish a cathode spot only for a very brief interval so that no arc is maintained during the time a reverse voltage is applied to the anode. Therefore no ionized gases will be present during this period and it is claimed this principle will result in an operation free of backfires. At least the original papers by Mr. Slepian elaborated on this theory and pointed out that this new principle of inducing an anode to fire would reduce the backfire tendency even though the anode is located directly above the cathode and not shielded by grids, baffles, or the like.

Therefore I was greatly astonished to notice from the cross-section view of such a rectifier (see figure 3) that not only is the same anode arrangement with shields used as in the conventional multianode rectifiers (see figure 1), which the authors call "an elaborate one," but the same ring insulator to hold the anode shield and a very complicated grid and shield with two baffles and a cathode with an ignitor. By comparing figures 1 and 3 these facts become apparent. Considering that for a 12-tank arrangement there are 12 ignitors and 12 cathodes instead of one as in the conventional type of rectifier, it, therefore, seems to me that the authors overstress the simplicity of this new rectifier design. The same comparison could be made considering the number of auxiliary devices for the ignition apparatus of both types, however, reference is made to figures 7, 8, and 9, each showing as many ignition and auxiliary devices as there are tanks.

The brief presentation about surges is very interesting and our observation on an ignitor-type working together with a conventional-type rectifier, when both were cooled with water at three degrees centigrade, showed the former to operate without any surges originating in the main arc. However, what was most surprising was that a great many surges were found to be originating in the ignition arc. It is not our experience that surges due to an unstable arc lead to backfires; several 3,000-kw units on the Long Island Railroad which were installed from six to ten years ago were subjected to numerous surges during the winter season because the cooling water was often below ten degrees centigrade during starting. There are, however, other reasons which made our company introduce recoolers in order to avoid having water of very low temperature enter the rectifier, as is the case when direct cooling is used. The same reasons would in some cases also necessitate a recooling system for these single-anode tank ignitor rectifiers.

I understand that not only is the life of these ignitors very short, probably due to the fact that the main arc in each cycle is for an instant concentrated at the base of the ignitor, but that these ignitors fail quite often, during normal operation, to establish the main arc, and one or more anodes and their respective transformer windings refuse to carry current for several cycles. In other words, every time an anode of a 6- or 12-phase transformer rectifier circuit fails to pick up current, the 6- or 12-phase transformer is magnetically unbalanced. It was a surprise to me to see ignitor rectifier installations where no precautions were taken to avoid the destructive effect of such abnormal operations. These effects may not yet be apparent since these rectifier installations are operating below full load and have not been in service very long. Frequent arc failures may affect the life of the secondary windings of the transformer, especially should such ignitor failures occur during heavy load.

It would have been very valuable if the authors could have given some data on how these ignitor rectifiers behaved during overload and what overload characteristics they show compared to the conventional multiple-anode rectifiers. Due to the lack of volume, the gases freed during overloads and the excess vapor pressure produced may have a very decided effect on the overload capacity of such small tanks.

It would have been very interesting if the authors could have made some comparison between the conventional multiple-anode rectifier tank, the sectionalized multiple-anode rectifier, and the ignitor single-anode tank, not only as to arc drop or efficiency, but also in regard to auxiliary equipment, power consumption of auxiliary

equipment, etc. This would have been very instructive because the same authors have fostered the introduction of sectionalized units—in other words, have repeatedly recommended the use of four multiple-anode tanks of 500- to 750-kw size instead of a 2,000-kw or 3,000-kw single-tank unit. All of you who have followed the rectifier development during the last few years will probably agree with me that the sectionalized rectifiers have not been used here nor abroad very extensively. Now this paper recommends the use of a further subdivision of units in tanks of single anodes. I do not want to infer that this may not be the final solution, but on the other hand I would like to call to your attention that the same optimism was expressed the last few years in regard to the sectionalized unit. Furthermore, we have to keep in mind that new features must be carefully weighed against the reliability of the well-tried and proved equipment.

J. H. Cox and G. F. Jones: Mr. Smith has presented some very interesting data on the capacities of glass-bulb rectifiers in service throughout the world and the bulb life being experienced with these units.

Bulb capacities up to 1,000 amperes are predicted, but it is interesting to note that the highest bulb capacity, for a commercial installation, listed in the discussion is 208 amperes. For large-capacity installations, this would indicate a large number of units with attendant complication and large space requirement. The overload rating standards listed in the paper are much lower than American standards for transportation service.

Glass is a poor heat conductor, which accounts for the very low capacity/volume ratio as compared with metal-tank rectifiers. Relatively large condensing surfaces are required in order to control the vapor pressure for given load conditions.

Except during the experimental stage, the ability to see what is going on inside the rectifier has little value. With modern relay applications, faulty conditions are easily detected and the cause exactly determined. With metal-tank rectifiers, any necessary repairs can be made on location by the regular maintenance personnel.

The glass-blowing art has not been developed in America to the point where large glass-bulb rectifiers can be made reliably. This makes any American user dependent on a European supplier. Contrary to Mr. Smith's statement, we are informed by a British user of glass-bulb rectifiers and by our European manufacturing associates that a regular setup is made for return of the glass bulb to the factory for re-evacuation, the user being supplied with special shipping cradles to minimize breakage during shipment.

For comparable service standards, the control functions for any type of rectifier are the same. Mr. Smith points out the simplicity of fuses in the anode circuits of glass-bulb rectifiers. This is simply an inexpensive device for localizing faults which could be used with any rectifier if service standards permit manual replacement of fuses.

Mr. Durand has asked regarding the energized grid in the ignitron illustrated and regarding comparative water consumption of the two types of rectifiers.

As used at present, the grid is connected to its associated anode through a resistor, in the large size ignitrons, to insure prompt pickup of the anode. It is not used for voltage control. This is so easily accomplished by control of the ignitor current that there is no need for controlled grids in the ignitron.

For given conditions of load and cooling-water temperature, the water consumption of the two types of rectifiers is comparable. The loss in the ignitron is lower, but for present designs, the discharge water temperature is also lower, the two factors practically counterbalancing each other so far as water requirement is concerned. Due to the use of copper cooling coils for the ignitron, except for very bad water conditions, a heat exchanger is not required to minimize corrosion. For direct water cooling, the water consumption is considerably less than for a unit requiring a heat exchanger.

Mr. Marti expresses surprise at seeing grids in an ignitron. As pointed out in our paper, a limited amount of shielding is used to take care of the transition period. As is natural with a new product, ample margin is taken in the use of these grids. It is expected that they will be reduced in the future. However, even in the present design, the amount of reduction of shielding can be realized by comparing the arc drop of 12-anode 3,000-kw 600-volt units. For the ignitron the figure is 18 volts and for the single tank design, approximately 30 volts. There is little likelihood of the shielding being reduced in the "well-established" conventional rectifier.

The relative simplicity of the anode structure of the ignitron cannot be realized by comparing figures 1 and 3. Figure 3 shows one anode of a 12-anode 3,000-kw 600-volt ignitron, whereas figure 1 shows one tank of a four-section 3000-kw 600-volt sectionalized rectifier which has a total of 24 anodes and which has by far the simplest anode structure of any rectifier at this rating. If a comparison is made with the anode structure of a 12-anode single-tank rectifier at this rating, for either grid control or non-grid control, the relative simplicity is quite pronounced.

The ignitron has a separate cathode for each anode. This cathode consists of a mercury pool in the bottom of the tank with a small quartz ring to confine the intermittent arc to the center of the pool. Note that there are no cathode insulators, no vacuum seals, no return mercury baffles, and no moving parts in the excitation system.

Mr. Marti's statement regarding ignitron circuit surges comes as a complete surprise to us. Surges in an arc originate when, for given conditions, the arc path is overloaded. If properly applied, the low-energy ignitron circuit of the ignitron will not surge nor will it be influenced by surges originating in an associated power arc. In the authors' experience, which comprises most of the past experience with ignitrons, there have been no surges in ignitron circuits.

The life of the ignitors is still to be determined. We now have over 100 ignitors in commercial rectifier service and have yet to experience a single failure of an ignitor in operation extending up to two years.

Pickup of an ignitron anode is just as reliable as for a multianode tank rectifier. For either type of rectifier, it is simply a matter of supplying the proper ionization to insure pickup. Ignitrons operate an inverter service with excellent reliability. Here, a single failure of an anode to pick up will result in a forward fire or short circuit. Extensive testing of both types of rectifiers in this service shows comparable reliability.

Failure of a thyratron excitation tube will result in failure of its associated anode to pick up. This results in magnetic unbalance of the transformer with tendency to saturate and with consequent increase in magnetizing current. The only result is increased heating of the transformer. This is a gradual temperature rise and is guarded against by transformer thermal protection. It could hardly be termed "destructive." An occasional failure of an anode to pick up is definitely of no consequence.

In actual railway service, a few failures of thyratron tubes have been experienced. The tubes have been replaced during normal inspection periods and in no case has any protective device been called upon to operate nor has service been impaired, even with tubes out of service for several days. Simple automatic means are available, if desired, to detect continuous misfiring of an anode.

The overload characteristics of the ignitron are equal to those of the conventional rectifier. Obviously they must be to meet the specified tests. They are somewhat superior in the higher, short-time overloads—above rating—because of the more open arc path and lesser tendency to surge.

The auxiliary power requirements are not an important item in determining rectifier efficiency. A vacuum pumping system requires less than one kilowatt and an excitation system for a tank rectifier or ignitron, three-quarters of a kilowatt or less. The efficiency curves shown in the paper include auxiliary power losses.

Contrary to Mr. Marti's statement, the sectional rectifier had an excellent reception following its advent, notwithstanding the fact that it was a new type in an established field. Since then, there have been as many sectional rectifier units installed in the transportation industry as single-tank rectifiers from any one supplier. The ignitron is receiving an even more gratifying reception.

Experience With Ultrahigh-Speed Reclosing of High-Voltage Transmission Lines

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THE basic principles of ultrahigh-speed reclosing of high-voltage transmission lines have been presented previously before the Institute.[1] Although, as has been explained, the effects and phenomena behind these principles are generally known, there are still a number of unknown factors that have not been explored fully and the effects of which need be known to make a thorough and scientific application of these principles. Among these are further data on the effects of breaker time and elapsed time between clearing of the arc and the re-energization of the circuits on the deionization of the arc and the re-establishment of the insulation strength of the surrounding area; data on the probable extent of and total time elapsed, as well as time between intervals of multiple strokes; knowledge concerning the effects of the quantity and the type of load on the likelihood of restriking or the failure of lines to hold when re-energized by virtue of system drifting away from synchronism; and, finally, data on the dead time needed on phase to phase faults to prevent restriking.

All of these, however, are elements in a problem which can reasonably be expected to be further developed as time goes on. They are all aspects of a problem that on the whole is well understood and on which further work is going forward. But while waiting for results from this work to materialize, there is no reason why the admitted great advantages in power-system operation of utilizing the main principle need be or should be waived because the problem has not been worked out yet with complete precision. As a matter of fact the authors believe that there is no high-voltage line today, if it is of any importance, that should be designed and installed on any other basis except on the basis of ultrahigh-speed reclosing. Since the presentation of the first data on this subject, three additional line sections, that is six terminals, have been equipped with ultrahigh-speed reclosing and a number of others, as will be brought out later, are under way. It is the purpose of this paper to describe the development of the equipment and the installation of these new terminals and to cite operating experience with all of them in so far as that is available.

Paper number 39-61, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 25, 1938; made available for preprinting December 17, 1938.

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1. For numbered reference, see end of paper.

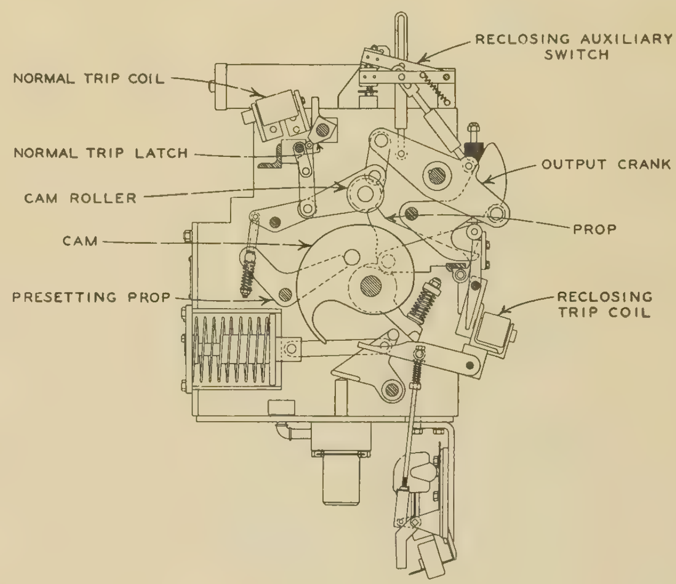

Figure 1. Schematic diagram of the new ultrahigh-speed reclosing mechanism for oil circuit breakers

The Elements of Ultrahigh-Speed Reclosing

The elements of an ultrahigh-speed reclosing setup are three. These are the high-speed breaker, the ultrahigh-speed mechanism and the high-speed relay system.

It is desirable that the circuit breaker be capable of interrupting a fault and de-energizing the line in the least possible time. So-called high-speed breakers available heretofore, in the United States at any rate, have had to utilize for this purpose a time of approximately eight cycles after the trip coil was energized. This does not refer to special designs. Within the last year more or less standard breakers having total time not to exceed five cycles have been developed and although faster times are expected, they are as yet not available today, in the American market at any rate. The work described herein has all been carried out so far on breakers having a time of approximately eight cycles.

The reclosing mechanism of the breaker has to be so designed that it is capable of reclosing the breaker with an elapsed time equal to no more than the minimum necessary to assure complete deionization of the arc and, therefore, assurance of no restriking on the one hand and minimum probability of loss of synchronism or loss of load on the other hand. Within the limits of eight-cycle operation of a breaker it has been felt heretofore that this time has to be of the order of a minimum of seven cycles but this has not been obtainable heretofore on mechanisms without running into stresses beyond what was desirable or practical in the standard breaker designs. As will be shown later on the actual performance so far this time has been from 10 to 14 cycles.

The relay system again must operate positively to clear the circuit on both ends in a minimum of time. Within the limits of breaker and reclosing speeds immediately in contemplation, it has been shown previously that this time should not exceed one cycle. No difficulty has been experienced so far in getting that with the utmost reliability.

In the earlier designs of ultrahigh-speed oil-circuit-breaker mechanisms two

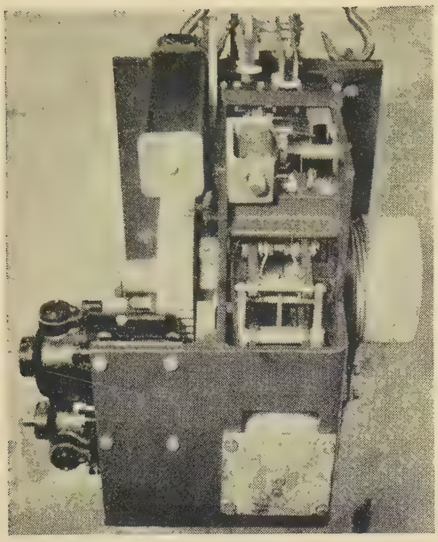

Figure 2. Front view of new ultrahigh-speed reclosing mechanism

mechanisms were provided for closing the breaker. One of these was a standard closing mechanism for normal operation and the other was designed to provide high-speed reclosing. These mechanisms were attached to opposite ends of a walking beam to the center of which the operating rod for the breaker contacts was attached. Energy for the high-speed reclosure was provided by a heavy spring which was reset automatically after each reclosure by means of a small motor. Each mechanism was provided with a trip coil and a transfer scheme was provided so that all tripping relays which would normally trip the reclosing mechanism were automatically transferred to the standard mechanism after a high-speed reclosing operation and remained there until the reclosing spring had been reset. The high-speed reclosing mechanism was also provided with an opening spring to speed up the opening of the breaker contacts. When the reclosing trip coil was energized this spring pulled the breaker contacts through a travel of about eight inches, at which point a latch released the closing spring, thus completing the reclosing operation. It was found that these mechanisms could be adjusted to give reliable operation with 18 cycles total elapsed time from trip coil energization to reclosure of breaker contacts.

Because there were two mechanisms required for each breaker, the total space needed for the complete equipment was considerably increased over that required by a standard electrically-operated breaker. Further, the space occupied by the high-speed reclosing part of the device was much greater than that used by the standard closing mechanism, so that the total space occupied by the complete equipment was more than doubled.

It was felt that this space requirement should be reduced if possible and at the same time the mechanism should be simplified in its operation. Keeping these objectives in mind, a new mechanism was designed and built which used the same motors both for normal closing and high-speed reclosing, thus eliminating the need for powerful reclosing springs, reducing the space required for the equipment, and considerably simplifying its operation.

A schematic diagram of the new ultrahigh-speed reclosing mechanism is shown in figure 1. As in the case of the earlier equipments these new mechanisms are provided with a standard trip coil and a high-speed reclosing trip coil. The closing motors drive a cam which operates the breaker output crank through a roller on its surface. At the end of the closing operation a prop falls into place and holds the breaker contacts closed. At the same time the cam is prevented from returning to the open position by the presetting prop. When the reclosing trip coil is energized the prop is removed and the breaker contacts begin to open. After the contacts have opened a predetermined amount, an auxiliary switch energizes the closing motors starting the cam revolving in a direction to close the breaker. The breaker opens until the cam roller comes in contact with the cam surface, the contact motion is then reversed, and the breaker recloses. Operation of the normal trip coil releases a trip-free toggle opening the breaker without a high-speed reclosure. As in the case of the earlier mechanisms, these new mechanisms permit breaker adjustment to reclose contacts in 18 cycles after the trip coil is energized and to do so with a considerably lessened strain.

Two views of this motor mechanism are shown in figures 2 and 3, and a view of the mechanism installed on a standard 138-kv oil circuit breaker in figure 4. The latter illustration is particularly striking when compared with a corresponding illustration of the original mechanism shown previously.

Ultrahigh-Speed Reclosing Installations on 132-Kv Lines of the Central System of the American Gas and Electric Company

The first 132-kv line on which ultra-high-speed reclosing was installed, was the 59.2-mile line between the Fort Wayne (Ind.) station of the Indiana and

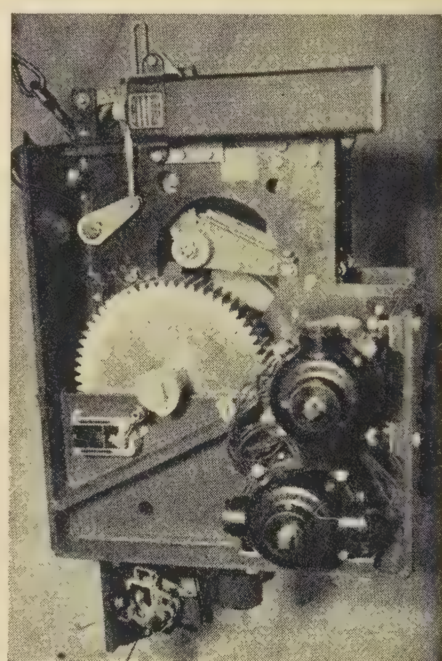

Figure 3. Side view of new ultrahigh-speed reclosing mechanism

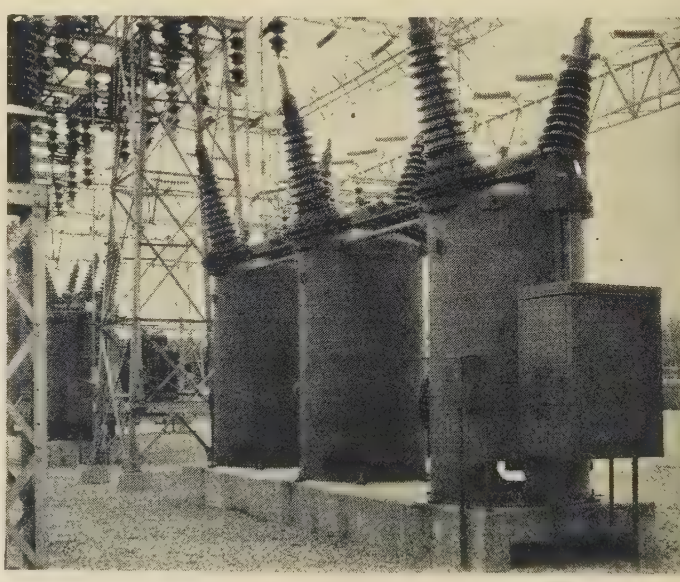

Figure 4. A 132-kv installation of the new ultrahigh-speed-reclosing breaker

Michigan Electric Company and the Deer Creek (Marion, Ind.) station of the Indiana General Service Company. This was placed in operation on May 17, 1936. Figure 5 shows a diagrammatic arrangement of the transmission lines in the area. It will be seen that these include a number of connecting lines comprising a loop circuit radiating from the Fort Wayne station and that they supply not only the Deer Creek station but also the Delaware station of the same company at Muncie, Ind. Although there is an interconnection at Kokomo, it is not of sufficient capacity to supply the entire load of the Indiana General Service Company when separated from the Fort Wayne station. Consequently the load here represents the general condition of a stub load fed from an interconnected system either by a single line or by a double-circuit line on a single-tower line. The solution for maintaining service continuity to this load was, as previously explained, felt to be the application of ultrahigh-speed reclosing, as a necessary part of which one-cycle carrier-current relaying was included. Behind all this was the idea that in case of simultaneous trouble occurring on the two circuits of the single tower line between Fort Wayne and Deer Creek, service restoration on the Fort Wayne-Deer Creek line would be made fast enough to avoid the normal consequences of a double circuit tripout.

The two ultrahigh-speed reclosing breakers used on this line were of the first design employing spring-operated mechanisms. The breakers were adjusted to give an over-all time of 20 cycles from the energization of the trip coil to the time the line was re-energized and that meant a dead time of approximately 12 cycles. The one-cycle carrier relay system gave an actual relay operating time of slightly less than one cycle.

The first operating results previously described and on which further data will be given in this paper were so successful that the same principle was applied on additional line sections. The sections chosen for equipping with ultrahigh-speed reclosing were three sections of a 350-mile double-circuit tie line between the Philo station of The Ohio Power Company, the Twin Branch station of the Indiana and Michigan Electric Company, and the Michigan City station of the Northern Indiana Public Service Company. Figure 6 shows diagrammatically these lines with various sectionalizing stations, generating capacities, and the synchronous condenser capacities installed at the various points. These three stations in turn tie in with very extensive 132-kv net-

Table 1. Summary of Actual Operations of 132-Kv Ultrahigh-Speed-Reclosing Oil Circuit Breakers

				Fault						Source Terminal						Load Terminal					
Number	Date	Time	Line Section	Type	Single or Double Line	Cause	Fault Arc Restruck	System or Load Affected	Line De-energized Time (Cycles)	Primary Amperes	Relay Time (Cycles)	Oil-Circuit-Breaker Clearing Time (Cycles)	Oil-Circuit-Breaker Reclosing Time (Cycles)	Total Time (Cycles)	Oil-Circuit-Breaker Retripped	Primary Amperes	Relay Time (Cycles)	Oil-Circuit-Breaker Clearing Time (Cycles)	Oil-Circuit-Breaker Reclosing Time (Cycles)	Total Time (Cycles)	Oil-Circuit-Breaker Retripped
1	6/ 6/36	8:31 p.m.	Fort Wayne-Deer Creek	Phase 3 to ground	Single	Lightning	Yes	No	16.5	300	1.0	6.5	19	20	Yes	1,500	1.0	6.5	21	22	Yes
2	6/17/36	5:31 p.m.	Fort Wayne-Deer Creek	Phase 3 to ground	Single	Lightning	No	No	13	281	1.0	6.5	18	19	No	1,552	1.0	6.0	22	23	No
3	7/23/36	6:02 p.m.	Fort Wayne-Deer Creek	Phase 3 to ground	Single	Lightning	No	No	13	1,170	1.0	6.5	20	21	No	647	1.0	8.0	22	23	No
4	8/28/36	8:59 a.m.	Fort Wayne-Deer Creek	Phase 3 to ground	Single	Lightning	No	No	12.5	563	1.0	6.5	20	21	No	1,165	1.0	6.5	22	23	No
5	5/26/37	3:41 p.m.	Fort Wayne-Deer Creek	Phase 3 to ground	Single	Lightning	No	No	13	937	1.0	7.0	20	21	No	1,140	1.0	6.5	21	22	No
6	6/ 5/37	2:15 p.m.	Fort Wayne-Deer Creek	Phase 1 to ground	Single	Lightning	No	No	12	300	1.0	6.5	20	21	No	1,725	1.0	6.5	21	22	No
7	6/24/37	7:17 a.m.	Fort Wayne-Deer Creek	Phase 3 to ground	Single	Lightning	No	No	13	1,500	1.0	6.0	20	21	No	690	1.0	7.5	21	22	No
8	6/24/37	8:42 a.m.	Fort Wayne-Deer Creek	Phase 3 to ground	Single	Lightning	Yes	No	14	1,125	1.0	6.5	20	21	Yes	863	1.0	7.0	21	22	Yes
9	7/ 3/37	2:30 a.m.	Fort Wayne-Deer Creek	Phase 1 to ground	Single	Lightning	No	No	14	1,690	1.0	7.0	20	21	No	700	1.0	6.5	21	22	No
10	2/19/38	9:31 p.m.	South Bend-New Carlisle	Phase 3 to ground	Single	Sleet	No	No			1.0		18	19	No		1.0		18	19	No
11	5/20/38	3:17 p.m.	Fort Wayne-Deer Creek	Phase 3 to ground	Single	Lightning	No	No	12	1,000	1.0	6.5	20	21	No	1,050	1.0	6.0	20	21	No
12	5/20/38	7:15 p.m.	Fort Wayne-Deer Creek	Phase 3 to ground	Single	Lightning	No	No	10	380	1.0	7.0	20	21	No	1,670	1.0	6.0	20	21	No
13	7/25/38	11:06 p.m.	Fort Wayne-Deer Creek	Phase 3 to ground	Single	Lightning	No	No	15	2,000	1.0	7.0	23	24	No	645	1.0	8.5	23	24	No
14	8/ 5/38	4:51 p.m.	No. 2 Twin Branch–Fort Wayne	Phase 3 to ground	Single	Lightning	No	No			1.0		18	19	No		1.0		18	19	No
15	8/10/38	8:26 p.m.	No. 2 Twin Branch–Fort Wayne	Phase 3 to ground	Double	Lightning	No	No			1.0		18	19	No		1.0		18	19	No

works totaling some 4,000,000 kw of generating capacity. The flow of power over this tie line on certain sections reaches above 100,000 kw at some times and for this reason it is obviously very important that the continuity of the line should not be interrupted, if at all possible, even in case of simultaneous trouble occurring on parallel lines on the same tower line. Hence it was only natural that with the promising results obtained on the Fort Wayne-Deer Creek line, the solution of ultrahigh-speed reclosing as a means to insuring continuity be tried here.

The 17-mile line between the South Bend and New Carlisle substations was equipped with ultrahigh-speed reclosing equipment and placed in operation on January 29, 1938. The 65.4-mile double-circuit lines between the Twin Branch generating station and Fort Wayne station were equipped with ultrahigh-speed reclosing equipments a little later and the equipments placed in service on July 25, 1938, on one line and on August 31, 1938, on the second line.

The six ultrahigh-speed reclosing breakers used on these three line sections were of the latest design, employing motor-operated reclosing mechanisms of the type shown in figures 2 and 3. All of these circuit breakers were adjusted to give an over-all time of 18 cycles from the

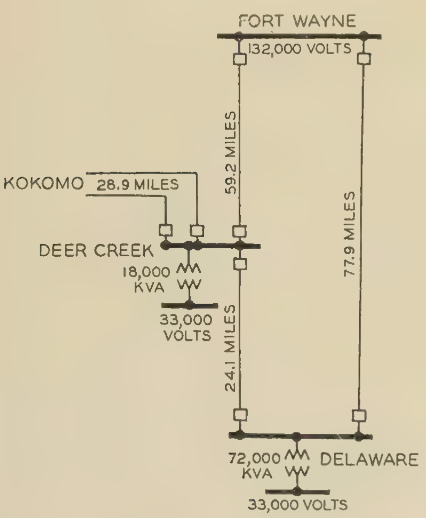

Figure 5. The 132-kv system of the Indiana and Michigan Electric Company radiating from Fort Wayne and supplying Deer Creek and Delaware stations of the Indiana General Service Company

energization of the trip coil and that meant a dead time of approximately 10 cycles. The one-cycle carrier-current relay system again gave the usual relay operating time of somewhat less than one cycle.

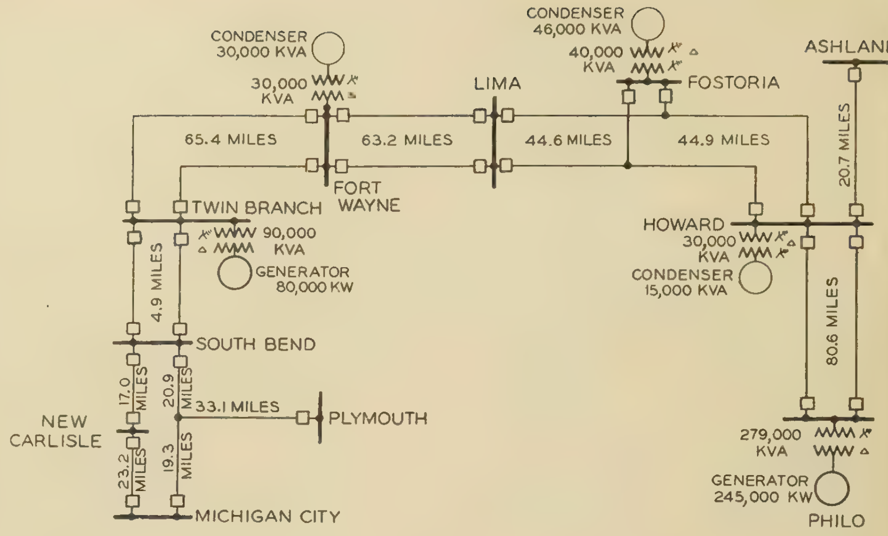

Figure 6. The 132-kv double-circuit tie line of The Ohio Power Company, Indiana and Michigan Electric Company, and Northern Indiana Public Service Company

Operating Experience

With the installation of ultrarapid reclosing equipment on the three line sections described above, there were operating during a portion of 1938 a total of eight 132-kv breakers so equipped and serving four line sections. Since their installation there has been a total of 15 operations on the four line sections. A summary of these operations, all of which except one due to sleet have been traced to lightning, giving the time, type of fault, total time of de-energization, and details with regard to fault current and breaker operation, is given in table I. It will be seen that in all cases flashovers occurred between a phase wire and ground, 12 of the flashovers being between phase 3 which is the top conductor and ground, one between phase 3 which is the middle conductor and ground and two between phase one which is the bottom conductor and ground. In 13 of the 15 cases the breakers on both ends of the line tripped and reclosed without the arc restriking and without any of the normal deleterious effects of a feeder outage being felt in any case. This point, however, needs further elaboration and this will be done below.

In two of the 15 cases the breakers failed to stay in after the first initial ultrarapid reclosure. At least one of these cases analysis shows to be due to multiple lightning stroke and it is quite likely that the other failed to stay in for the same reason. Although in cases 2–7 inclusive and in cases 9, 10, 11, 12, and 13, no normal deleterious effects due to breaker outage and line de-energization were observed, it will be noted that in each of those cases only single line faults occurred. Hence no full test of the efficacy of the steps taken and the equipment installed was obtained in any of these situations.

It is, however, interesting that in the case of the Fort Wayne-Deer Creek line where prior to the installation of a one-cycle relaying system, plus the ultrarapid reclosing setup, approximately 25 per cent of lightning flashovers involved both circuits, in more than two years that have elapsed since the installation of the ultrarapid equipment, not a single double-circuit flashover occurred although no other steps of any kind were taken on the line. Whether that is due to chance or whether the reason is to be found in the speeding up of the relaying and breaker action just the necessary amount to prevent involvement of the second circuit by the first to flashover, is something on which additional data will have to be obtained. But the two striking operations that put to at least partially complete test the principles attempted to be developed in this method of operation of high-voltage lines, were obtained in cases 14 and 15, and these deserve more full discussion.

In case 14, the system setup previous to this operation was a case of a single circuit tie between Twin Branch and Fort Wayne stations and a long weak tie line between the Fort Wayne and Plymouth stations. Referring to figure 6, the Twin Branch oil circuit breaker on one of the lines at Fort Wayne station was open due to oil-circuit-breaker revamping to ultrahigh-speed reclosing, and the breaker on the other end of the line at Twin Branch station was closed supplying a tap-off load. A 205-mile tie line between the Fort Wayne station and the Plymouth station by the way of Marion, Kokomo, Lafayette, Oakdale, and Monticello sta-

tions was closed. A lightning flashover between phase 3 conductor and ground occurred on the tie line between Twin Branch and Fort Wayne stations, causing the breakers at both ends of the line sections to trip from carrier-current relays and to reclose immediately without restriking without the loss of any load or system disturbance. The speed of operation was sufficiently fast to prevent the opening up of the weak tie line between Fort Wayne and Plymouth stations, which invariably occurred when Twin Branch station was separated from Fort Wayne station before the installation of high-speed-reclosing circuit breakers on these lines.

In case 15, the system setup was normal before this operation in that both lines between Twin Branch and Fort Wayne were in service as well as the weak tie line between Fort Wayne and Plymouth being closed. During a lightning storm

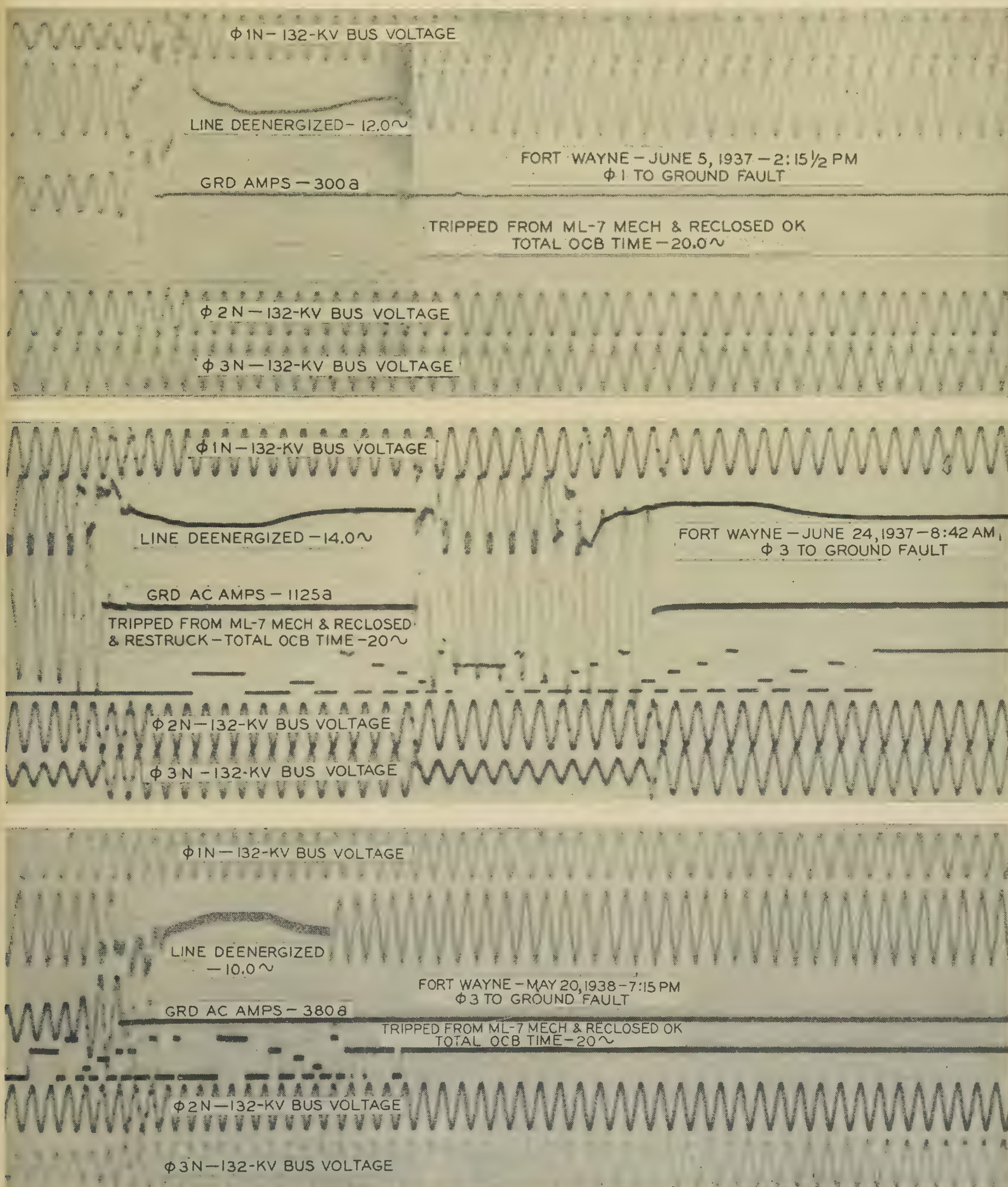

Figure 7. Oscillograms of actual operations on Fort Wayne-Deer Creek line

simultaneous trouble occurred on both Twin Branch-Fort Wayne lines, causing one of the lines to trip at both terminals from carrier-current relays and to reclose immediately without the arc restriking. On the other line installation of high-speed-reclosing breakers was not as yet completed, and both terminals of this line tripped from instantaneous overcurrent relays since carrier-current relaying was not in service at the time. In this case no loss of load or system disturbance was experienced, and, in addition, the weak tie line between Fort Wayne and Plymouth stations did not open up.

The fact that in similar situations as those that occurred in cases 14 and 15 the system invariably pulled apart gives every reason to believe that the speeding up of the reclosing process can in similar situations be made to give the very desired result of having the systems opened and yet reclosed before either loss of voltage is felt on any part of the system or before the systems drift apart sufficiently to make necessary the introduction of considerable delay until they can be resynchronized. Obviously further expe-

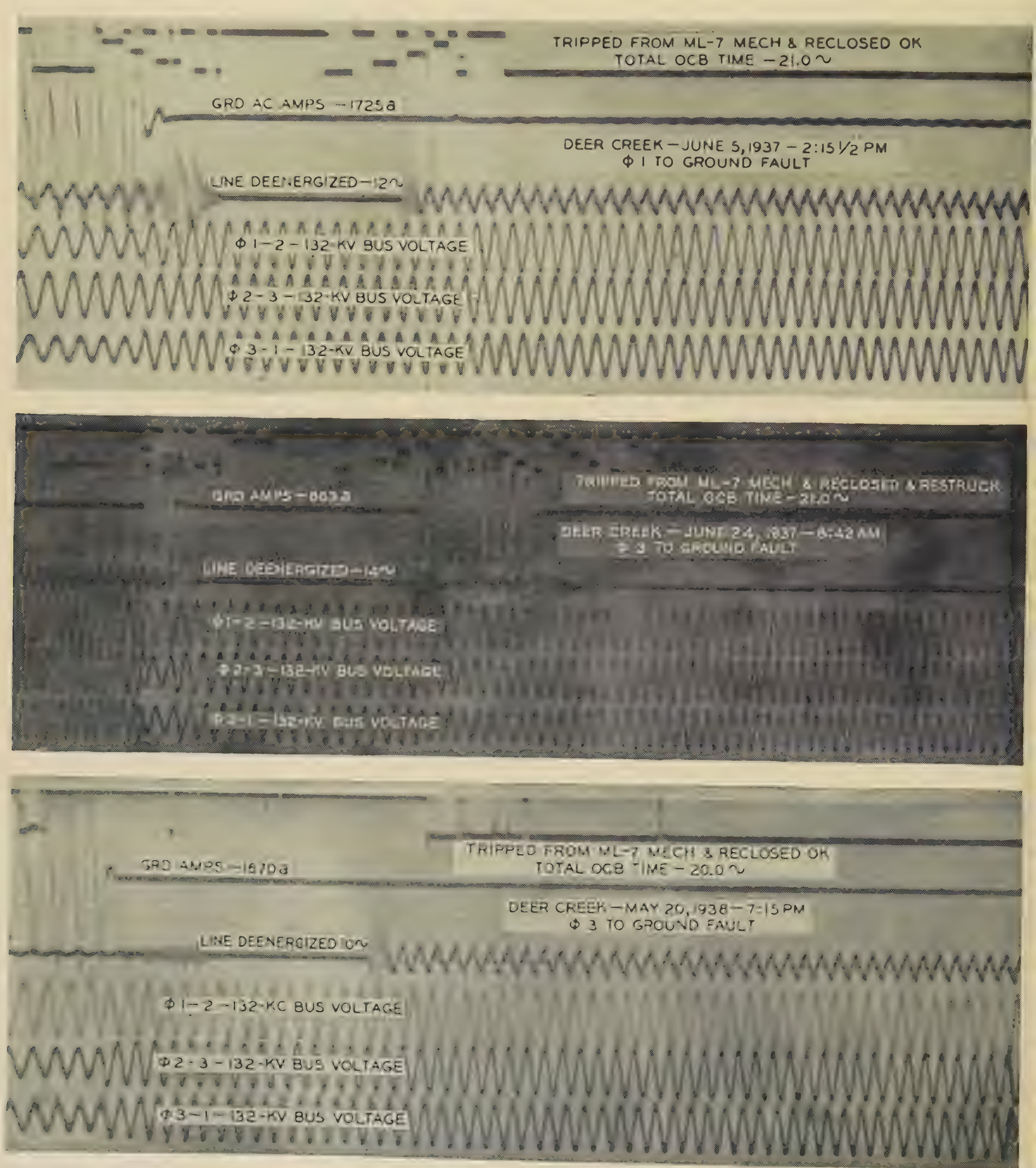

Figure 7 (continued). Oscillograms of actual operations on Fort Wayne-Deer Creek line

rience is necessary in similar situations while carrying loads more nearly within the stability limits of the line. But with the installation of additional equipments it is hoped to be able to present information on the performance under such conditions.

The oscillographic records of the performance of the systems under these conditions are extremely illuminating. In figure 7 are shown three sets of oscillographic records showing system performance in cases 6, 8, and 12. Referring to the record of case 6 it will be noticed that a short circuit occurred close to Marion on phase 1 and that the line remained de-energized for a period of approximately 12 cycles and reclosed without any further disturbance.

The oscillogram of case 8 shows the line voltage on the third trace from the top of the record taken at Deer Creek. This is obtained from a bushing potential device and it will be noticed that at least three cycles before the line was re-energized, potential of possibly three times normal appeared on the line, eventually dying out. The inference is very strong that a lightning discharge took place to the line and that the effect of that was to cause the line to flashover dynamically when it was re-energized some two cycles after the discharge itself had apparently disappeared. The appearance of the same voltage is shown clearly also on the record taken at Fort Wayne, except to a lesser extent. A possible explanation of that is that the voltage at Fort Wayne is measured by a regular instrument-type potential transformer.

The record of case 12 was obtained on May 20, 1938, and shows a successful reclosure of the Fort Wayne-Marion line. It will be noticed in this case that the fault was very close to Deer Creek and after a line de-energization of ten cycles, reclosure was entirely successful.

New Installations

The 80.6-mile double-circuit 132-kv lines from the Philo Generating station to the Howard substation (see figure 6) are both at present being equipped with ultrahigh-speed-reclosing circuit breakers in conjunction with one-cycle carrier relaying and it is expected that these will be placed in operation the early part of next year. This is another step in making this 350-mile tie line immune to interruption in continuity for simultaneous trouble occurring on two lines on the same tower line.

Plans are under way to install ultra-high-speed-reclosing circuit breakers on the 77.9-mile 132-kv line between the Fort Wayne station of the Indiana and Michigan Electric Company and the Delaware substation of the Indiana General Service Company (see figure 5). It is expected that these installations will be put in operation before the next lightning season. The aim in this case is to install ultrahigh-speed-reclosing circuit breakers on both of the 132-kv supply circuits radiating out from the Fort Wayne station to insure uninterrupted service to this important load in case of simultaneous trouble occurring on both of these lines, one of which it is hoped will remain closed after the first reclosure.

A number of other points on the American Gas and Electric system still remain where principal source of supply comes in either over a single-circuit line or over a double-circuit line running on the same right of way without some loop backup. In all of these cases it is expected over the next year to install ultrarapid reclosing as fast as the breaker and relaying problems in connection therewith can be properly worked out and taken care of.

Conclusions

The results obtained so far, it appears to the authors, definitely warrant the following conclusions:

1. On high-voltage overhead lines outages caused by lightning can be materially reduced by ultrarapid reclosing. The originally[1] expected figure of 75 per cent seems conservative in the light of the additional experience obtained since that time.

2. Apparently two-circuit flashover, on double-circuit transmission lines properly equipped with ground wire, when the two ends are equipped with ultrarapid-reclosing breakers is materially reduced. It does not appear that the ultrarapid reclosure element can be a major contribution in this regard and it would appear, therefore, that this is in a large measure the result of a speeding up of the relaying and circuit interrupting process. More information on that is needed and it is hoped to gather it over the ensuing years.

3. It appears definitely that not only can service continuity materially be improved by the installation of ultrarapid reclosing on lines feeding isolated areas, but the use of ultrarapid reclosing on tie lines between two major generating systems will result in line flashover having a minimum effect on the continuity of power flow. Apparently even when handling power close to the stability of the line ultrarapid reclosure when properly functioning can be made to bring two systems together without the necessity of a wait for synchronization and without the danger of system drift to a point where reclosure without synchronization produces any deleterious effect or fails to keep the two systems together.

4. The experience with ultrarapid reclosure seems to indicate the need for a complete re-examination of the concept of the switching process on any important high-voltage circuit, consisting of relaying, de-energization, and re-energization of the circuit. If ultrarapid reclosure will accomplish, in a vast majority of cases, the results here indicated, it would appear logical to consider that under all cases the proper cycle of operation is one in which ultrarapid reclosure to the extent of one reclosure ought to become standard practice and that only in exceptional cases ought a period of waiting longer than the absolute minimum required for deionization, be permitted between de-energization and reclosure.

5. The work carried out on high-voltage systems seems to indicate that very much similar results can be obtained on intermediate and low-voltage lines by utilizing the same principle. The exploration of that field is a problem for the future. The authors themselves hope to be able to contribute something to that.

Reference

1. ULTRAHIGH-SPEED RECLOSING OF HIGH-VOLTAGE TRANSMISSION LINES, **Philip Sporn** and **D. C. Prince**. AIEE TRANSACTIONS, volume 56, 1937, pages 81–90.

Discussion

S. B. Crary (General Electric Company, Schenectady, N. Y.): The benefits which have been obtained from quick switching and relaying have been fully demonstrated in practice and by system studies and stability analyses. More recently immediate reclosure of standard circuit breakers has been applied with pronounced success to feeder circuits and to a limited extent to tie lines. Now Messrs. Sporn and Muller present operating data showing the improvements which they have obtained by combining quick switching with rapid reclosure by means of special, fast, breaker mechanisms and relaying. Those of us who are engaged in making system studies are impressed with the benefits which may be derived from quick switching and quick reclosing and the possibilities it opens up for improving the reliability of service. Undoubtedly, in almost every major system, there are circuit-breaker locations and system conditions for which quick switching and rapid reclosure would be or are of material benefit in improving service. Some circuit-breaker locations and system conditions make quick clearing and reclosing more desirable and favorable than others. These locations and conditions are generally recognized and can be determined quite accurately by a system analysis.

A system analysis based on operating records of the types of faults to be expected along with information similar to that present in the paper, can be used to predict the expected improvement in system performance. There are now generally available a-c network analyzers and developed procedures for making such studies. In recent years a better understanding has been obtained of the transient performance of ma-

chines and systems undergoing system disturbances and out of step conditions, both from an analytical and an operating point of view. No small part of this new knowledge has been contributed by the automatic oscillographs. As an aid in determining the electromechanical oscillations to which rotating equipment is subjected during out of step and pulling into step conditions, the differential analyzer has also been put into use. All of these methods of analysis have contributed to a better understanding of system and machine performance and have made it possible to predict more accurately their behavior. This knowledge can be used to advantage in the rational application of quick reclosure to power systems.

With simultaneous circuit-breaker tripping by means of carrier-current relaying and an over-all time of 18 cycles from energization of the trip coil to breaker reclosure an accomplished fact, it becomes evident that such equipment may be more generally used to prevent loss of synchronism between two systems for even more severe conditions than that reported in the paper by Messrs. Sporn and Muller. It should be recognized, however, that there do exist limitations as to how much power may be carried through a disturbance with a successful reclosure for a given set of conditions. The more important factors are the synchronizing strength of the electrical tie, the effective system inertias, the fault location, number of phases involved, fault duration, and reclosure time. However, the effect of these factors may be evaluated and the power limitations predetermined with fair accuracy by analysis.

A factor of considerable importance favoring the reclosing principle which is becoming more fully recognized is the ability of systems to pull together even after being closed-in out-of-phase or definitely out of step. The conditions and factors which allow for this resynchronization are determined to an appreciable extent by the automatic control devices, voltage regulators, and governors, which are attempting to hold the voltage and speed of the individual parts of the system. There have been several cases where this ability of systems to regain synchronism has been fully demonstrated. An important factor in such resynchronization is that the relaying should not be sensitive or operate during the out of step or pulling into step process. Such a consideration may tend to influence the relaying philosophy of a system which intends at some future time to take full advantage of quick reclosure. This constitutes an important reason why it is advisable to give immediate careful consideration to the reclosure problem. The factors which allow systems or parts of systems to resynchronize are also amenable to analysis. Since system performance can be studied evaluating the effect of the factors influencing system operation following reclosure, it is believed that in the future more consideration and study will be given to this problem in a similar manner as was done in the past, in studying the effect of quick clearing of faults as a factor in system and machine stability.

It has been suggested in the case of two parallel circuits which are equipped with quick switching and reclosing that a control scheme be used which will allow for quick clearing and reclosing of the faulted circuit, if only one is involved, when the load to be carried through the disturbance is above the stability limit of one circuit. However, in the case in which the load to be transmitted is below the stability limit with one healthy circuit left in service, reclosure on the faulted circuit is delayed in order to reduce the possibility of reclosing on an uncleared fault at an inopportune time of the system oscillation. Accordingly, in this case it is conceivable that the probability of proper system operation is actually increased by delaying the reclosure. However, if operating near or above the stability limit of one circuit it may be advisable to reclose as quickly as possible. The justification of using this type of control is determined among other factors, by information of the nature which Messrs. Sporn and Muller are accumulating, such as the percentage of faults which clear during the breaker reclosure time, the number of phases and circuits involved in the fault, etc. Their results are of considerable value and should encourage the consideration and study of a more general use of rapid reclosure.

E. E. George (Tennessee Electric Power Company, Chattanooga): In the first part of their paper Messrs. Sporn and Muller use one argument which might bear considerable emphasis. They state that the electrical industry ought not to wait for a complete and precise evaluation of all factors involved before utilizing high-speed reclosing. Most of the transmission companies in the Southeast would agree that ultrahigh-speed reclosing is desirable on all high-voltage lines; nevertheless they have not waited for high-speed-closing breakers. So-called immediate reclosing was put in use in the Southeast about five years ago and has since been considered standard practice on all transmission and distribution lines since that time. It has now been applied to practically all existing lines.

High-speed reclosing is of much more value today than it was a few years ago. Lines are being built better and there is increased probability of coming back in service "OK" after an interruption. Many years ago when a large percentage of line failures resulted in burn-downs and permanent trouble it was "OK" to do switching on the basis "where there's no hope there's no hurry." With breakers of the closing speed standard until recently, the reclosing time has varied from 75 cycles at 154 kv to 15 cycles at 2,300 volts with the majority of operations between 45 and 25 cycles.

It is now standard practice of The Tennessee Electric Power Company to reclose immediately on *both* ends of tie lines if pilot wire or power carrier protection is available. This is applied regardless of lower-voltage loops, transmission loops through the rest of the system or through interconnecting companies, etc., although it might not be adequate if a generating plant were tied to the system with only one line.

This reclosing is being used at both ends of 110-kv, 44-kv, and 11-kv ties, most of which are short and protected with pilot-wire relays. Wherever the relaying is too slow to permit instantaneous reclosing on both ends it is used on one end with synchronism check or dead bus reclosing on the other, thus restoring the line in a few seconds.

Many of us have talked about faster closing of oil circuit breakers for several years but outside of Mr. Sporn and his associates we have done very little about it. In answering a questionnaire on the subject of reclosing about five years ago our company stated that we would reclose breakers in ten cycles if the equipment were available. We still stand by this opinion and feel that reclosing even faster than Mr. Sporn is using would work out satisfactorily in most cases. Operating experiences have shown that little weight should be given the fear of multiple lightning strokes or reignition of an arc across transmission insulator strings. Multiple lightning strokes are largely confined to certain rare types of lightning storms, but do not seem to occur frequently, at least when we are talking about multiple strokes a few cycles apart rather than a few microseconds apart. In this area our worst lightning occurs in wind-shift storms during the passage of a cold front, and the lightning is generally accompanied by high winds. This tends to prevent reignition of an arc.

Experience in the southeastern area would indicate that all new transmission lines should be designed to utilize the development discussed by Messrs. Sporn and Muller. However, on existing transmission lines great advantage can be obtained with existing relays and breakers by applying immediate reclosing.

A. J. A. Peterson (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors are to be commended for the confidence with which they have proceeded to equip their important 138-kv transmission lines with high-speed reclosing equipment as described in this paper, and the results described in their paper well justify their work in this direction.

As pointed out in the paper, the practical limit on high-speed reclosure is determined by the time of complete outage required for deionization of the original fault path, which time, of course, increases with the service voltage. This confirms the findings of Griscom and Torok ("Keeping the Line in Service by Rapid Reclosing," *Electric Journal*, May 1933).

Even though the limit on reclosing speed for the circuit breaker is to a large measure determined by this deionization time, faster

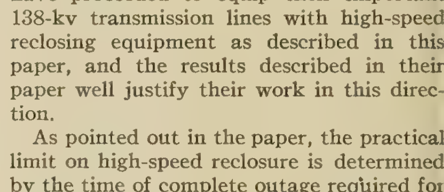

Figure 1. Early high-speed motor mechanism

arc interruption will obviously permit faster over-all reclosing time, and therefore, better maintain system stability.

With the fault current interrupted, the next step in restoring service and maintaining stability is to reclose the breaker quickly. The mechanics of a quick reclosing cycle are: first, fast opening of the breaker; second, stopping the opening stroke; and third, closing the breaker. Trip-free action must be provided so that the opening action will not be unnecessarily delayed by the mechanical or magnetic drag of the closing means. We have been working on such a mechanism for several years. Figure 1 of this discussion shows a high-speed circuit-breaker operating mechanism. It consists essentially of two sets of springs, one for closing and the other for tripping. These springs are charged by motor. Drive from the springs to the breaker mechanism is through a "capstan" clutch which gives very quick engagement at any part of the stroke, thus permitting fast closing at any desired point on the opening stroke, as well as trip-free operation at any point on the closing stroke. In reclosing, the closing means is controlled to initiate closing of the breaker before the full open position is reached. The mechanism thus quickly stops and reverses the breaker, and carries it back to its closed position.

A further development of the mechanism is shown in figure 2. This mechanism is a direct motor drive, using the same type of "capstan" or band friction clutch. The high-torque high-speed motor drives a helical band friction clutch through suitable worm gearing. This clutch freely rotates around a drum attached by suitable shaft, crank, and levers to the breaker operating rod. In order to close the breaker, the motor is energized and quickly comes up to speed. At the same time, the friction band is snubbed into action around the breaker drum by a low-energy magnetic face plate. This couples the breaker directly to the motor drive. Trip-free action is obtained by simple de-energizing the snubber, thus releasing the breaker drum from the motor friction band, and permitting the breaker to

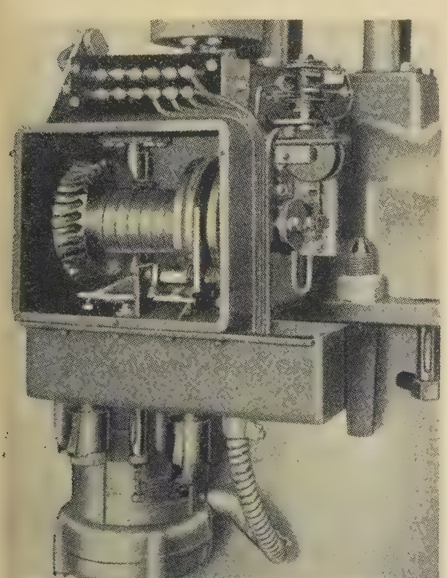

Figure 2. High-speed motor mechanism with "capstan" clutch, front view

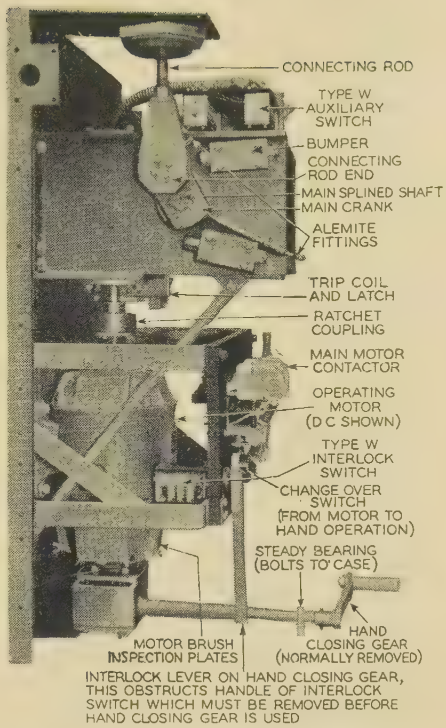

Figure 3. High-speed motor mechanism with "capstan" clutch, for 138-kv oil circuit breaker, side view

open freely through its normal accelerating springs. The same design of mechanism for 138-kv breakers is shown in figure 3. Because of its inherent ability to get into action quickly, this mechanism is well adapted to provide high-speed reclosing on a fast-opening breaker.

D. C. Prince (General Electric Company, Philadelphia, Pa.): Engineers are constantly under pressure to find ways of providing better service at reduced cost. Any improvement in line availability and freedom from interruption is a step in this direction.

Overhead ground wires, improved grounding, additional insulation, Petersen coils, expulsion gaps, duplicate lines, and ultrahigh-speed tripping and reclosing are all means of improving continuity of service. Each of these steps affects continuity of service in a different manner; each has its cost, each must be judged in the last analysis by field experience.

Ultrahigh-speed reclosing is a means toward service improvement which presented originally a good many problems. First, ultrahigh-speed-reclosing means had to be devised; then laboratory and field tests had to be made to determine how fast reclosure would be useful. And then, finally, it was necessary to find how much of the possible theoretical gain might be realized in practice. The authors of this paper are to be congratulated and thanked for furnishing an answer to this last and crucial question.

The improvement in service appears to compare favorably with that obtained by other means. High-speed reclosure is applicable to any type of temporary fault whether phase to ground or phase to phase, and whether from lightning, sleet, or birds. It does not eliminate tripouts, but judging from the paper, does reduce loss of load which is the important element. Since breakers are required in any case, the cost of securing this improved service by high-speed reclosure is very moderate considering the improvement obtained.

It will be very interesting to see to what extent high-speed reclosing can be used as a substitute for other more expensive means of assuring service continuity.

Percy H. Thomas (Federal Power Commission, Atlanta, Ga.): Mr. Sporn and his associates have added another to the long series of fundamental and classic papers, on the problem of the adequate control of the flow of electric energy in heavy power circuits, and in the usual style—brief, to the point, and adequate.

A look ahead as to the ultimate critical factors in this most important aspect of the control of the flow of electric energy may be of interest.

What can be done to increase the length of the time of interruption that may be permitted without loss of synchronism in the system? With the complexity and infinite variety of conditions met in utility power circuits, no general particular method of extending this time seems probable. On the other hand, if and when the use of reclosing breakers is extended and the holding of synchronism becomes critical, no doubt a study of individual situations will show effective means to secure a material improvement. With the present available methods of recording transient phenomena in power circuits and the means for mathematical analysis, such a study offers little difficulty.

What can be done to shorten the time required for the extinguishment of the arc when the operation is to interrupt a flashover?

As stated in the paper, the prompt interruption of the current after the initial breakdown is of the greatest importance. The amount of matter heated and the accumulated energy generated in the arc might increase as the cube, or higher, with a proportionally rising current. Since the opening oil breaker introduces an important resistance in the line long before the current is interrupted, the saving in the accumulation of heat in the flashover arc will be very greatly reduced by quicker operations. That portion of the duration of the arc in which the current is a maximum would be materially shortened. But the saving is double: first, the saving of time in the opening operation; second, the lessening of the interval of waiting, with the current interrupted, for the arc to die out.

That this rapidity of action is important and very potent, is shown first by the well-known experiments of Nicholson on quick-acting arc switches and fuses, and, second, by the effectiveness of the low-voltage high-speed d-c breaker.

Such a phenomenon as the multiple lightning flash should presumably require a different treatment to avoid handicapping all the opening operations by a lengthy waiting period to meet the occasional special case.

Can anything be gained in opening the circuit by basing the action of the opening relay on the rate of rise of the flashover current? This "rate of rise" will be greatest

at the start and will of course be at a maximum only when the total impedance of the arc circuit is small; that is, when the arc current is potentially very large. If the secondary of a line current transformer is connected to a relay of high resistance, preventing a saturating flow of current in the transformer, the relay would be subjected to a voltage proportional to the rate of rise of the arc current. This should represent some gain in relay action.

What advantages may be hoped for from changes in the mechanical construction of the breaker? No doubt with the excellent performance so far secured, evolution for the immediate future and for the immediate expansion of utility use should continue with substantially the same model. However, in seeking the ultimate best layout, other radically different forms should be considered, as in the end somebody will get a better type if the possibility exists. It is well recognized that at times less advantageous types of apparatus have been long used in practice in the electric art where better ones were potentially available.

In these breakers, the interests of size and cost and upkeep put a very great premium on light moving parts. Designs with possibly five to ten per cent of the mass of those here used would have a great advantage in these matters of cost and size, as well as speed. A radial blade carried by a rotating shaft, as already used in some designs, offers a tempting field for the designer. The very speed of action attainable might eliminate the necessity of some of the present arc-suppressing features. It may well be that a sufficiently rapid extension of the rupturing arc in the breaker through oil, under an initial moderate pressure, might have a miraculously increased effectiveness.

Is the growing aggregate capacity of generating stations and the necessity of increasing rupturing capacity likely to develop into a limitation in the use of reclosing breakers? It would be a natural inference from the reports of tests and construction difficulties in these "Sporn" papers that the economic rupturing capacity limit was very likely being rapidly approached in this type of construction.

There are at least two avenues of escape to be considered: first, the beneficial effects of more rapidly moving parts in breakers; second, the adoption in the layout of large electric power systems to cut the concentration of too great short-circuit power on any one bus or connection point. This can be accomplished, as is well known, oftentimes by paralleling large generators only through the distribution systems. Where there are several large generating stations on a network, the loose coupling of units can be accomplished, at least in a new system, with very little added cost. In such a case a great limitation of the present demands for flexibility in a major power station would be a minor handicap in operation, though requiring some sacrifice of habits of thought and a different point of view on the part of the station operator.

The writer has long advocated this approach to the solution of the threat made by the concentration of generating capacity.

In closing this discussion, the writer desires to point out what seems to him the natural conclusion from the availability of the reclosing breaker, that is, that, assuming that a satisfactory method of handing flashovers has been found, new line construction should be based on this fact and no effort be made to make lines flashover-proof. Reductions should be made in the heights of ground wires, in the length of insulator strings, clearances, etc. When the vast increase in the expense of overhead construction in the last few years, and the present pressure for lowered cost are remembered, the opportunity should be welcomed. It is not unreasonable to hope that ground wires may ultimately be largely omitted. Of course engineers and operators will be very loath to build lines with less insulation than now provided, and it will be agreed that changes should come step by step in the light of resulting experience, but, in the view of the writer it is the duty of our leading system managers, in appropriate cases, to consider carefully and initiate this policy. It may be pointed out that a low-insulated line protected with reclosing breakers may have a higher factor of safety, from the point of view of operation of the system, than a superinsulated line without such protection.

If we may add to the reclosing breaker protection, a system layout such that there is always the capacity in the network to serve the load with any particular link disconnected for a while, the result is a system of reserve, which provides against almost any form of interruption, whether lightning, mechanical failure, or operative error, and a system which does not require any supertypes of construction. One interruption a month on any unit in such a system would presumably not disturb operation and would be good practice.

While comments and suggestions of this sort may be sound and of a great potential value, the credit for any benefits resulting must go to those who have the foresight, the courage, the stamina, and the facilities for making the initial application.

V. M. Marquis (American Gas and Electric Service Corporation, New York, N. Y.): One reclosure which has not been discussed by the authors and which gave an excellent check on the ability to reclose between two large systems without losing synchronism when transferring heavy load occurred on November 16, 1938, on the South Bend-New Carlisle 132-kv line. The location of this line and its relation to the other parts on the system is shown in figure 6 of the authors' paper.

A fault on the Plymouth tap caused a trip-out of the South Bend-Plymouth-Michigan City line when about 80,000 kw was being delivered east from Michigan City, 50,000 kw of which was delivered to South Bend and 30,000 kw into the Plymouth substation. The tripping out of this line threw the entire 80,000 kilowatts on the Michigan City-New Carlisle-South Bend circuit and, immediately following, the South Bend-New Carlisle line tripped and reclosed successfully while carrying the load of 80,000 kw. Figure 4 of this discussion shows a graphic record of power flow over the line at New Carlisle substation; the load change due to the opening of the Michigan City-Plymouth-South Bend circuit and the reclosure on the South Bend-New Carlisle circuit is shown at *A*. In figure 5 is shown a graphic record of the 132-kv bus voltage at South Bend substation. This meter is equipped with a high-speed trip attachment, but the surge was not sufficient to operate it; however, the record indicates that the fault on the Michigan City-Plymouth-South Bend line caused a voltage dip of some six volts and the reclosure a dip of only about two volts.

In this particular instance, the operators at Michigan City did not know that the South Bend-New Carlisle line had tripped out until some time later when the operators at South Bend and Michigan City were interchanging data on relay targets. This is no reflection on the alertness of the operators but rather, as I see it, a compliment to the ultrahigh-speed reclosing equipment.

Previous calculations had incidated that there would be no difficulty in reclosing this circuit when carrying a load of 80,000 kw over it and this operation bore this out. This operation, it is believed, was an excellent demonstration of the possibilities of ultrahigh-speed reclosing on important high-voltage ties.

With reference to the high-speed reclosing on the Twin Branch-Fort Wayne circuits, it is of interest to note that a load is tapped off one of these circuits midway between Twin Branch and Fort Wayne. During the reclosures that have occurred on this line, the loads fed by this tap have not been disturbed.

The authors in conclusion 3, in discussing improvement to service due to high-speed reclosing, point out that it is apparently possible to reclose even when operating near the stability limit of the line. It is perhaps pertinent to point out that the time available for reclosing of a tie line between

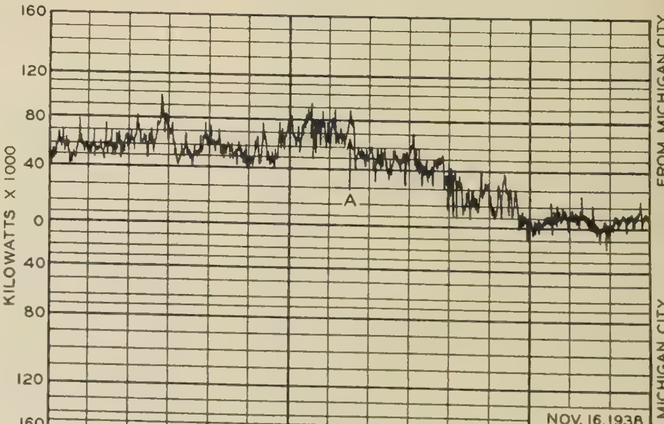

Figure 4. Power interchange on South Bend-Michigan City tie recorded at New Carlisle substation

Reclosure at *A*

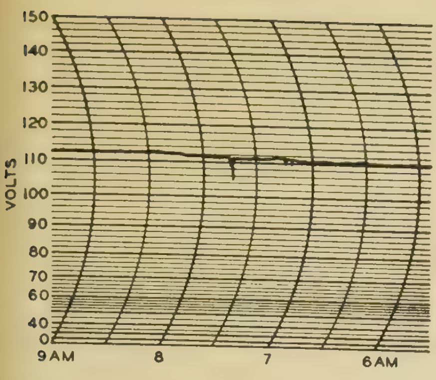

Figure 5. The 132-kv bus voltage at South Bend substation

Voltage dip not sufficient to operate high-speed trip on voltmeter

two systems without losing synchronism is dependent upon several factors, one of which is the relative inertias of the two systems. Whereas as much as 30 cycles might be available when reclosing between two large systems, calculations indicate that something under ten cycles would be available when reclosing, for example, between a hydro plant and a large steam system, if reclosure is to be made without losing synchronism. However, there has been some experience in reclosing and holding the systems together even when they are out of phase. Further experience may indicate that there will be no particular difficulty in such cases even with the present speeds available for reclosing.

K. B. McEachron (General Electric Company, Pittsfield, Mass.): It is interesting to compare the data on multiple strokes with the performance figures obtained for successful automatic reclosure operation reported by Messrs. Sporn and Muller. Figure 6 of this discussion is taken from a paper entitled "Multiple Lightning Strokes—II" (K. B. McEachron, AIEE TRANSACTIONS, volume 57, 1938, page 510) and shows the operations of expulsion protector tubes as measured oscillographically in those cases where two or more successive operations were involved. The record shows tube operations on the 132-kv Roanoke-Glenlyn and Roanoke-Danville lines of the American Gas and Electric Company. Table I of the paper referred to shows a total of 184 discharges, which operated protective tubes during the four-year period of study beginning with 1934.

Of these 184 discharges, 52 were multiple, and from figure 6 of this discussion 10 are seen to involve a longer time than 20 cycles, or a little over five per cent of the total strokes which were of sufficient magnitude to cause tube operation, which no doubt also means that sufficient potential was encountered in most cases to have caused an insulator flashover.

If the total time is reduced from 20 cycles to 15 cycles, as an illustration, the number of times in which a single reclosure would not be satisfactory is increased to six per cent. This indicates that the shape of the curve is such as to show that the deionization time becomes controlling rather than the multiple discharges, from the point of view of decreasing reclosing time.

However, I do not believe that the above gives a clear picture of the true situation, because the data which we have available with Boys camera photographs, taken both in Pittsfield and in New York in the Empire State investigation ("Lightning to the Empire State Building," K. B. McEachron, *Journal* of the Franklin Institute, February 1939) since 1934, indicate that an appreciable percentage of the successive discharges of a multiple stroke will be continuing. That is to say, the current will persist for a considerable period of time after the initiating discharge has decreased to a relatively small value. It has been shown, for instance, that currents as great as 250 amperes may persist for as long as 0.4 second.

In the case of data shown in figure 6 of this discussion, the transformer neutrals were grounded, with the result that the direct current of the continuing discharge finds a ready path to ground when interrupted by the expulsion action of the protector tube at the point of inception. With the operation of the circuit breakers, the line is left isolated from ground, if high-voltage switching is used, with the result that the continuing current will hold on as a d-c arc over the faulted insulator string. The occurrence of this type of discharge tends to decrease the number of successful operations to be expected using one reclosure below that which might otherwise have been expected.

Perhaps some indication of the most pessimistic view might be obtained from a consideration of the results of strokes to the Empire State Building, in which, out of a total of 67 photographed, 27 had a duration of more than 0.33 second, or 20 cycles. On this basis, 40 per cent of the breaker operations would not have been successful. However, these results are undoubtedly influenced by the fact that many of the strokes began at the building rather than at the cloud, which is the usual case for transmission lines.

I am inclined to the view that the second oscillogram of the authors' figure 7 is a case of this sort, in which a continuing discharge caused the line to be in a faulted condition at the moment the breaker closed.

It seems to me that, for the present, until more accurate data are available, with a dead time as long as 14 cycles and a total time of 20 cycles, if a fault exists at the time of reclosing, it will be as a rule the result of a continuing discharge. If it occurs a cycle or more after reclosing, then it would appear to be either a multiple discharge or another stroke.

In lieu of any more accurate information, I would suggest a figure of 20 per cent as representing about the number of unsuccessful operations to be expected on the line with which Messrs. Sporn and Muller have experimented, as a result of multiple or continuing strokes.

Although the protector tube has its limitations, it does perform successfully on multiple strokes with long times included between the first and last discharge, and it ought to be free from the effects of continuing strokes on grounded-neutral systems.

It will be of interest to engineers to learn of the further experience which Messrs. Sporn and Muller will secure with automatic reclosing, and I hope that they will continue to secure oscillographic data which will help materially in determining the cause of unsuccessful reclosure.

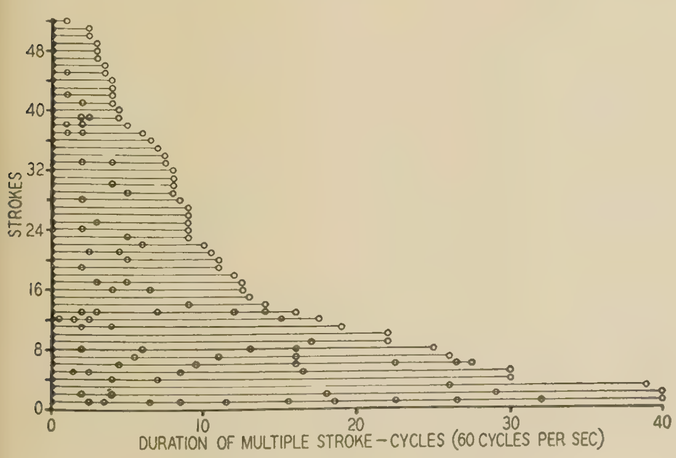

Figure 6

S. B. Griscom (nonmember, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors have presented most interesting data on a power system operating procedure which is probably destined to find an extensive future use. While calculations can be made to determine whether two parts of a system will be held together after a high-speed reclosure, they do not take the place of the assurance obtained by actual operation and the proof that some obscure factor has not been omitted from consideration.

In reading this paper, I looked over the illustrations before proceeding to the text, and after examining the Fort Wayne oscillograms of the case 8, June 24, 1937, fault, I concluded that this was an example of a restrike due to insufficient deionization time. This was based merely on the fact that in both the initial fault and the subsequent reclosure, the fault was from phase 3 to ground, as evidenced by the lowest voltage being on phase 3.

Upon reading the text, it was found that the authors had concluded that the evidence pointed a repetitive lightning stroke. This conclusion was inferred from the simultaneous oscillogram at Deer Creek where a disturbance on the line-voltage trace was noted three cycles before the actual reclosure. The restrike was interpreted as being due to the residual ionization from the lightning stroke persisting during the three intervening cycles before the circuit breaker reclosed.

It may not be possible to establish definitely whether in this particular instance the

restrike was caused by residual 60 cycle ionization or by residual ionization from a repetitive lightning stroke during the de-energized period. However, the principle is important because if the former, it will have a bearing on the allowance to be made for de-ionization time, while if the latter, since repetitive strokes are a random phenomena, nothing can be done about it.

Mr. Torok and I collaborated on an article "Keeping the Line in Service by Rapid Reclosure" in the May 1933 *Electric Journal*, and a large number of high-speed movies of 60-cycle arcs were taken in the preparation of data. I have just re-examined some of these to get an idea as to the length of time evidence of the 60-cycle arc remained. It was not uncommon to find that incandescent gases remained as long as 30 cycles after the current had been interrupted. However, as the oscillographic results showed, it was rare for the arc to restrike after 12 cycles, although it was a random occurrence. The evidence is that the flashover path gained dielectric strength because the ionized gas had drifted from the proximity of the electrodes by convection currents or action of wind, rather than because the gases had become sufficiently deionized again to withstand the applied voltage.

Such evidence that I have been able to locate indicates a different action in the case of arc paths due to lightning. Messrs. Brookes, Southgate, and Whitehead in AIEE paper 33–44 made tests indicating that power follow from lightning was affected by the magnitude of the 60-cycle voltage at the instant of the surge current. In tests on "De-ion" protectors, it is found that surges taking place near voltage zero are much less likely to result in power follow. This is a somewhat different circumstance, of course, since the discharge is confined. However, both examples seem to indicate that the arc space from lightning strokes recovers dielectric strength much more rapidly than that from 60-cycle arcs being measured possibly in hundreds of microseconds rather than in thousands.

Although the Deer Creek oscillogram for case 8 was not very distinct, there appears to be another surge on the "line de-energized" trace, about four cycles later than the cessation of the reclosure ground current. There also seems to be slight evidence of a counterpart on the Fort Wayne record, but this is only about two cycles later than cessation of the ground current. The duration of the ground currents was not the same at both stations so this may account for the difference. These disturbances are similar to those attributed to the repetitive stroke, which if that theory is correct, may indicate still a third stroke.

Philip Sporn and **Charles A. Muller:** Mr. Crary's discussion gives some idea of the general groundwork which must be performed to predict results to be expected from ultrahigh-speed reclosure, a very necessary step in justifying it economically. It is interesting to note that in the case of parallel lines equipped with ultrarapid reclosure it may be desirable to delay reclosure for a fault on one line if the total load is below the stability limit of a single line. This condition was considered in the case of the Twin Branch-Fort Wayne lines, but because of the additional complications in control which are involved it was decided to wait until operating experience should more clearly indicate its desirability.

Mr. Thomas has made some stimulating comments as to the possible future lines of investigation in improving the design and effectiveness of ultrahigh-speed-reclosing breaker applications. It is hoped that breakers will soon be available which will considerably reduce the amount of time required to extinguish the arc, and that this will, in turn, allow a reduction in the length of time the line must be de-energized, as Mr. Thomas suggests. This possibility was referred to in Messrs. Sporn and Prince's AIEE first paper on "Ultrahigh-Speed Reclosing of High-Voltage Transmission Lines," in which tests were cited that indicated that by reducing the arcing time from eight cycles to two cycles the time required for the line to be de-energized was reduced by about 25 per cent. In regard to Mr. Thomas' suggestion of employing a "rate-of-current-rise" relay as a possible means of decreasing somewhat the relay time required the authors believe that the present relay time of one cycle could not be materially reduced, and, since this is only a small portion of the total arcing time, any small gains in this time would be of minor importance in the over-all reclosing cycle.

Mr. Marquis has described an additional reclosure to those listed in the paper, which probably represents the most severe operating condition so far encountered. The fact that this reclosure was successful and it had been indicated from previous calculations that it would be, gives added confidence in the accuracy of the preliminary analysis as well as concrete evidence of the value of the equipment.

Mr. George brings out the point that the Tennessee Electric Power Company has been employing immediate reclosing as standard practice on all transmission distribution lines for the past five years, and that his company now recloses immediately both ends of tie lines equipped with carrier current or pilot wire relay systems. This is excellent operating practice but it needs to be emphasized that immediate reclosure at best is from two or three times longer in time than ultrarapid reclosure on high-voltage transmission lines, and that with this longer time of interruption, synchronism would probably invariably be lost between systems not having an extremely large amount of inertia if the line is carrying any appreciable load. However, it can still be of value if the systems have the ability to pull in step even though closed out of phase, as pointed out in Messrs. Crary's and Marquis' discussions.

From Mr. Griscom's comments on figure 8 of the paper, it appears that he has concluded as a result of laboratory tests that the cases where the 60-cycle power arc will restrike after 12 cycles would probably be extremely rare. It is especially interesting that he apparently believes the arc will not restrike after 12 cycles because of the probability that the ionized gases will have been swept out of the path along which the arc would probably be established rather than due to the absence of deionization. As Mr. Griscom points out, it is important to know whether the reestablishment of the arc is caused by a continuation of the 60-cycle ionization or by a multiple lightning stroke, or, as Mr. McEachron also suggests, by the possibility of a continuous lightning stroke. Incidentally, Mr. McEachron's discussion brings up a most important point in connection with the entire problem of lightning protective apparatus and the effect on it of the continuous lightning stroke. It is obvious that a great deal of our thinking as regards the requirements of lightning protective equipment will have to be considerably modified if such equipment is actually being subjected to continuous flow of current from lightning strokes of from 0.3 to 0.4 second.

It will be interesting to observe the actual operating data as it is gathered over the years and determine how the percentage of unsuccessful operations actually obtained compares with the 20 per cent which Mr. McEachron suggests as being probable. As has already been indicated heretofore, that has been about the percentage of unsuccessful performance, but there seems to be reason for belief that the percentage might be reduced with the speeding up of the breaker process. Operating performance, however, over the next few years ought to throw further light on this.

The authors were very glad to learn through Mr. Peterson of the work his company is doing toward developing another type of ultrahigh-speed reclosing breaker. While he has not mentioned the operating time for this mechanism, it is to be presumed that it will be as fast or faster than those described in the paper. It will be of considerable interest to learn more about this development and whether the design will permit modernization of existing breakers or will only be applicable to new ones. Actual operating results will also be awaited with interest.

Mr. Prince has reviewed the factors that have to be kept in mind in a system designer's attempts to continue to provide better service while reducing the cost of rendering it. The ultrarapid reclosing breaker seems to be one of the most valuable tools developed in many years that can be utilized in accomplishing this. But, of course, more experience will be required before one will be able to evaluate accurately all its advantages and disadvantages.

Out-of-Step Blocking and Selective Tripping With Impedance Relays

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ASSOCIATE AIEE

WHEN two interconnected power systems pull out of step the relative values of voltages and currents are such as to indicate a three-phase fault somewhere in the interconnection, which if in the zone of high-speed impedance or reactance relays will cause them to trip their associated oil circuit breakers. Although this phenomenon has been recognized for some time, and various blocking schemes suggested,[3,4] it has been generally accepted that when high-speed relays are involved, carrier current or pilot wires giving simultaneous control of the relays at both ends of a protected section are required to discriminate between a fault and an out-of-step condition.[2,7] This paper describes a scheme developed for an important tie circuit of the Gulf States Utilities Company, whereby out-of-step blocking and selective tripping are obtained with existing impedance relays and without the use of carrier current or pilot wires.

Paper number 39-81, recommended by the AIEE committee on protective devices, and presented at the AIEE South West District meeting, Houston, Tex., April 17–19, 1939. Manuscript submitted January 31, 1939; made available for preprinting March 13, 1939.

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The authors wish to acknowledge the valuable suggestions of W. A. Lewis, formerly of the central-station engineering department, H. A. Travers of the switchgear engineering department, and members of the relay engineering department of the Westinghouse Electric and Manufacturing Company. They also express their appreciation to R. O. Hopkins of the Gulf States Utilities Company for his assistance in making the field tests and preparing the data for the paper.

1. For all numbered references, see list at end of paper.

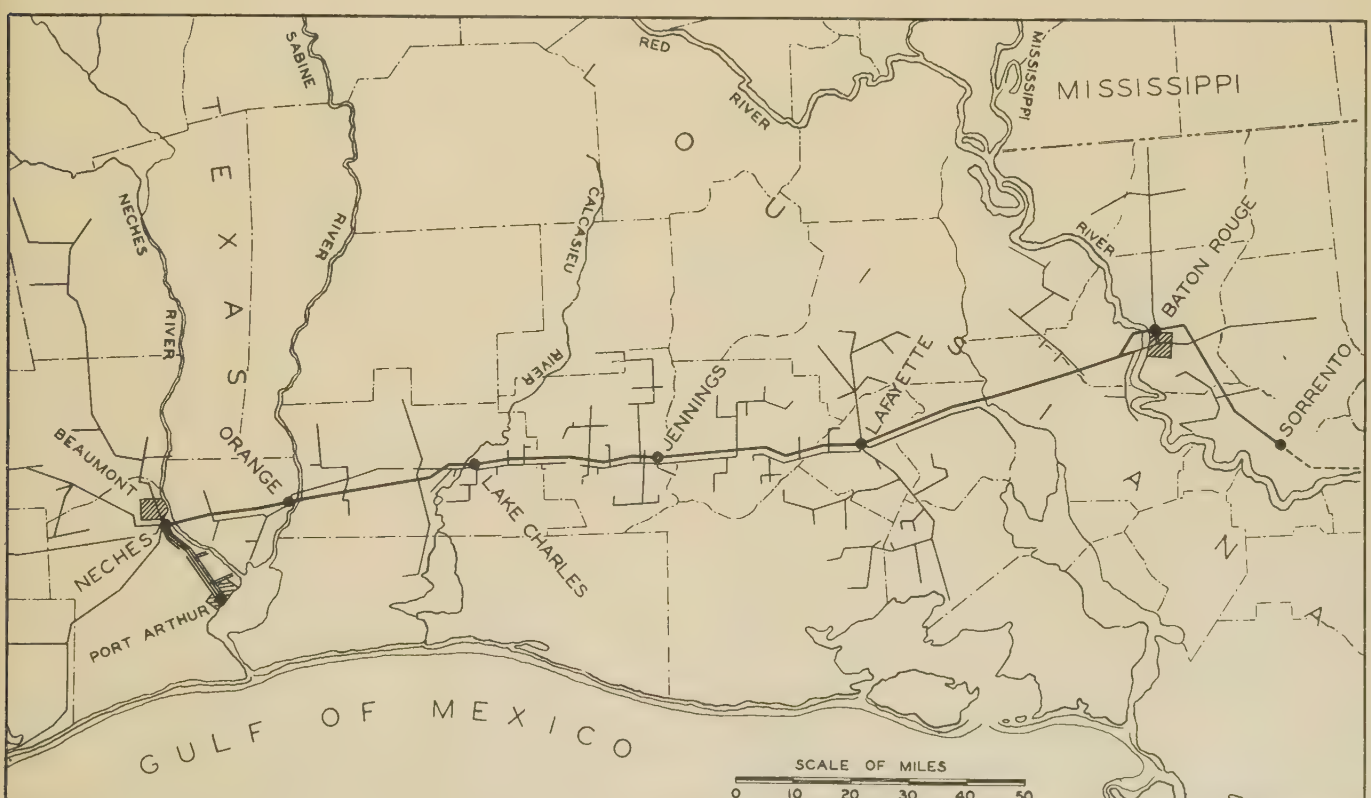
The Problem

The intervening load between the Neches and Baton Rouge generating stations of the Gulf States Utilities Company is supplied duplicate power by means of a 66-kv sectionalized tie circuit between the two stations as shown geographically in figure 1, and schematically in figure 2. Normally, power is fed into both ends of the line and tapped off to loads at each sectionalizing point. The division of load between the two generating stations varies with the season, depending on the total system load, steam requirements, etc. In case of a fault on this line, the faulted section is segregated and the two systems continue to supply uninterrupted power to the intervening load by operating independently until the fault is cleared and the interconnection re-established.

The high-speed impedance line relays and directional ground relays located at each sectionalizing point clear faults on the tie line with very little disturbance to the loads. However, opening the interconnection may cause an excessive load shift on the two plants, depending on which section is segregated. It is therefore important that the interconnection be opened only when absolutely necessary, and then, if possible, at a selected point.

Transmission-line faults, other than those which can be prevented by reasonable measures, will occur occasionally and must be tolerated. However, in addition to faults, instability occasionally develops due to disturbance elsewhere on the system, and the Neches and Baton Rouge plants pull out of step, causing the relays to open one or more sectionalizing breakers. Opening the interconnection at one point because of an

Figure 1. Interconnecting 66-kv transmission circuit between Neches and Baton Rouge steam generating stations



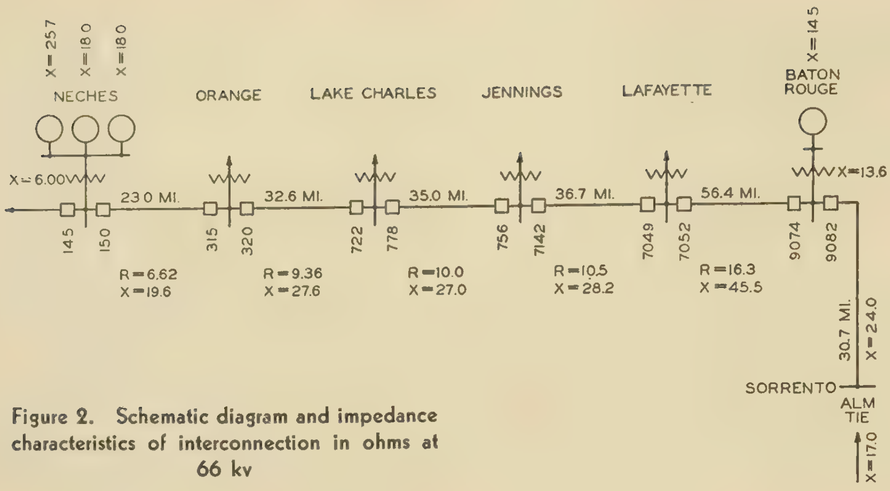


Figure 2. Schematic diagram and impedance characteristics of interconnection in ohms at 66 kv

out-of-step condition does not cause a loss of power to any load but is undesirable because of the usual change in load division between the two systems. Opening more than one breaker in other than one section would cause a loss of all load between the two breakers. The problem was therefore to prevent more than one breaker from opening during an out-of-step condition and to make it possible to select the breaker to be opened under the particular operating condition. Furthermore, it was desired to accomplish the out-of-step blocking and selective tripping with a minimum change to the existing relay equipment and without the addition of carrier current or pilot wires.

Relay Characteristics

The type *HZ* high-speed impedance relays which were installed in 1931 at all sectionalizing points of the 66-kv tie circuits between the Neches and Baton Rouge generating stations have been described in previous publications and are quite generally understood.[1] However, since they are so closely associated with the out-of-step blocking scheme, their characteristics will be briefly reviewed.

Each relay consists essentially of three high-speed impedance elements, a timer, and a directional element. Three relays, one per phase, are used for each oil circuit breaker. In this particular installation, each impedance element and the directional element are connected to receive delta current as shown vectorially in figure 3*A*. The impedance elements receive delta voltage in phase with the currents at unity power factor and the directional element receives voltage that lags the current 60 degrees at unity power factor.

The pull of the current coils tending to close the contacts is opposed by the restraining pull of the voltage coils so that the net force on the impedance elements is proportional to the ratio of voltage to current as shown in figure 3*B*. The curves indicate the relative balance points of the impedance elements, that is, the ratio of voltage to current at which the pull of the current coil just overcomes the voltage restraint. At any ratio less than the balancing ratio the contacts will close and at any ratio greater, the contacts will be held open. Any one of the three impedance elements will close its contacts in one cycle or less for a voltage-current ratio within its balance point. The first impedance-element contacts are connected in series with the directional contacts to close the trip circuit instantly. However, the second and third element contacts, in addition to being connected in series with the directional contacts, are each connected to a separate set of timer contacts which can be adjusted independently to give any desired time delay up to 180 cycles. The timer motor is started by the operation of the third impedance element.

The ratio of the voltage to the current at the relay location during a line fault is equal to the impedance to the fault so that the impedance elements can be set to balance at any desired impedance within their range. As shown in figure 3*D*, the first element Z_1 is set to balance at 80 per cent of the first section impedance, the second element Z_2, at 50 per cent of the second section impedance and the third element, Z_3, at 25 per cent, of the third section impedance. The first element of all relays trips in one cycle or less. The time settings of the relays at the different locations vary from 20 to 35 cycles for the second elements and from 100 to 120 cycles for the third elements.

It is evident from figures 3*B* and *D* that any voltage-current ratio within the balance ratio of Z_1 will close the contacts of Z_1, Z_2, and Z_3. Any ratio greater than the balance point of Z_1, and less than that of Z_2 will close Z_2 and Z_3 contacts. Any

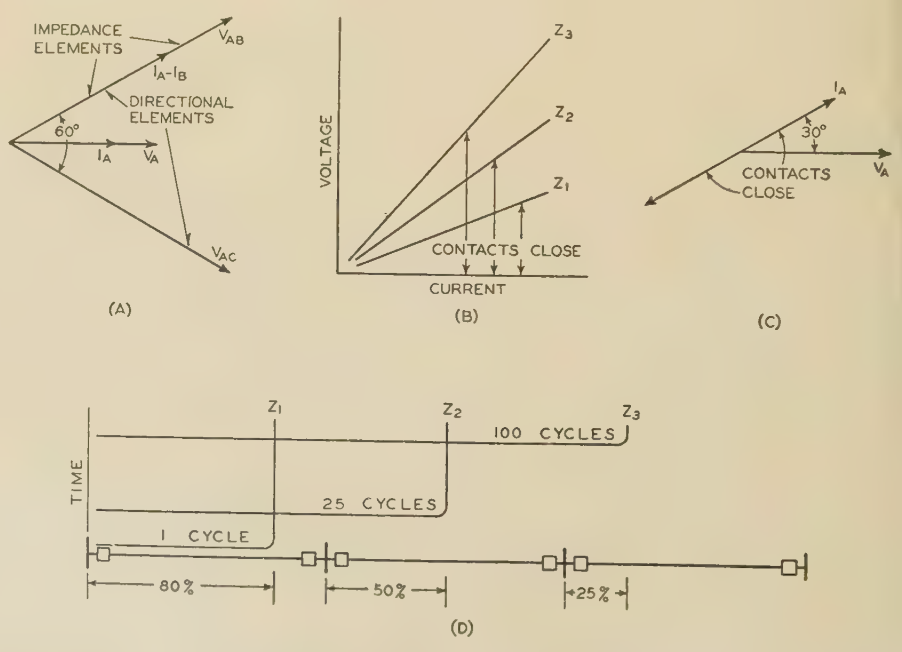


Figure 3. Operating characteristics of HZ impedance relays installed at all sectionalizing points

A—Vector relationship of voltages and currents applied to relay elements at 100 per cent power factor

B—Relative characteristics of impedance elements

C—Angle zone between line current and line-to-neutral voltage that directional contacts close

D—Time-distance setting of relays

ratio greater than the balance point of Z_2 and less than that of Z_3 will close Z_3 only.

The directional element has a true wattmeter characteristic, that is, there will be a closing torque on the contacts when the current applied to the element either leads or lags the applied voltage by less than 90 degrees. Since the directional element is connected to receive delta voltage which lags the applied delta current by 60 degrees at unity power factor, the phase relation of the phase current with respect to phase voltage for a closing torque is 30 degrees lead to 150 degrees lag as shown in figure 3*C*.

From the above, it can be seen that the manner in which the impedance relays operate during an out-of-step condition depends on the ratio of voltage to current, the relative phase angles of the voltage and current, and the length of time that the contacts of the impedance elements remain closed.

Analytical Investigation

In order to determine the effect of an out-of-step condition on the relays at the various sectionalizing points, an analysis was made of system voltages and currents during a complete slip cycle between systems. This information is also supplemented with oscillograms obtained with an automatic oscillograph located at the Neches generating station.

The part of the system considered consists of the Neches generating station at Beaumont, the 66-kv sectionalized tie circuit, and the generating station at Baton Rouge which is normally tied in with the Arkansas-Louisiana-Mississippi system at Sorrento. The impedances in ohms at 66 kv are shown in figure 2. A typical operating condition is two generators, number 1 and number 2, at Neches and two generators at Baton Rouge. Because of the relatively high line impedance the results would not differ greatly for other generating conditions.

The internal voltages (voltage back of transient reactance) of the generators at Neches and Baton Rouge, all of which are of the turbine type provided with high-speed voltage regulators, will remain substantially constant during a fault or out-of-step swing. Also, since power is normally fed into the line at both ends, the internal voltages at both stations will be about equal. The load current or line-charging current during the fault or out-of-step period will be relatively small. When the out-of-step condition is caused by an external fault located near one of the generating stations, the 66-kv bus voltage will drop so that, if the fault is of long duration, it will have considerable effect on the circulating current and line

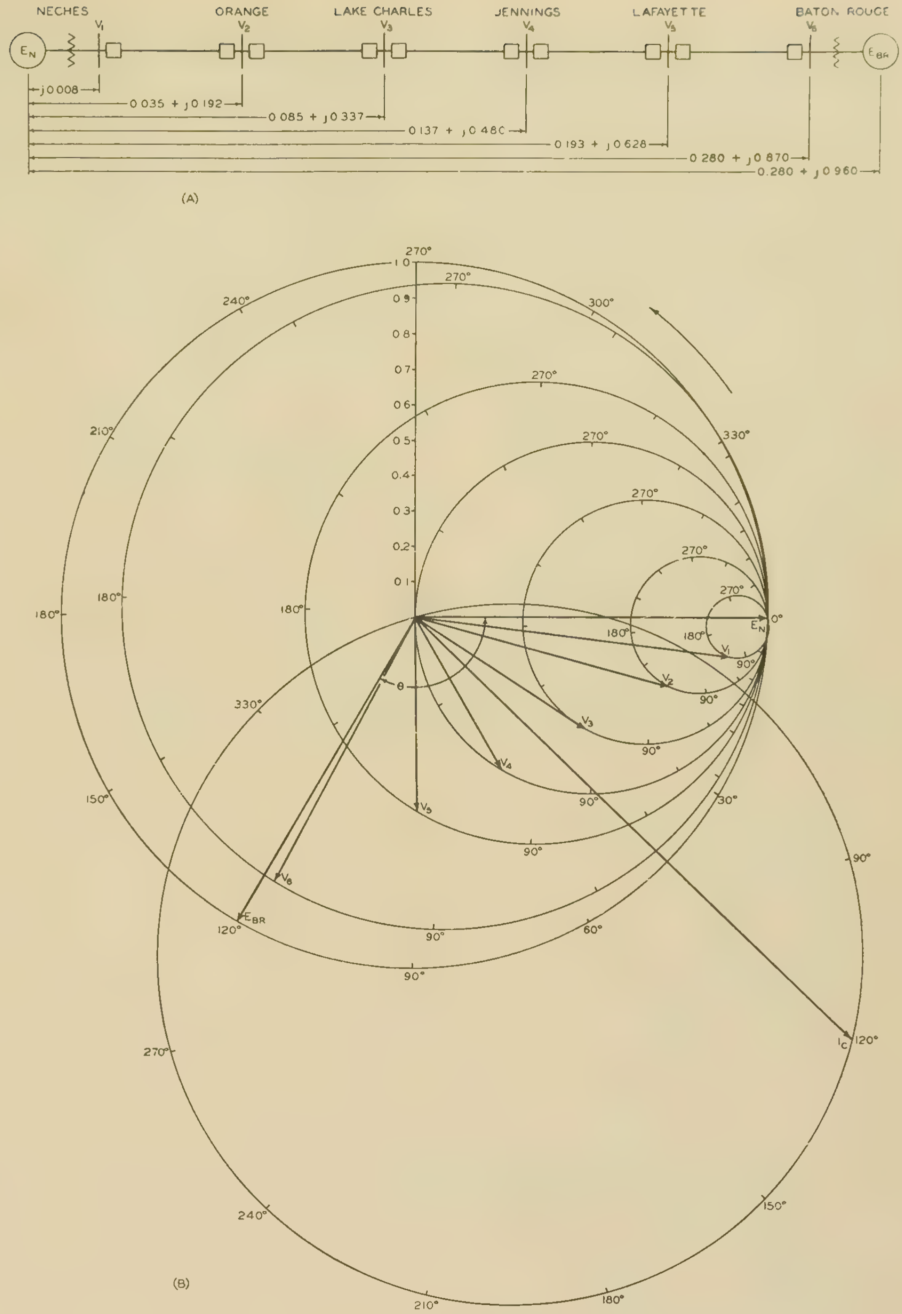


Figure 5. Circle diagrams of current and voltages at relay locations

A—Unit impedances to sectionalizing stations

B—Circulating current I_c, and line-to-neutral voltages at sectionalizing stations for complete slip cycle between Neches and Baton Rouge. Vectors are shown for 120-degree displacement angle

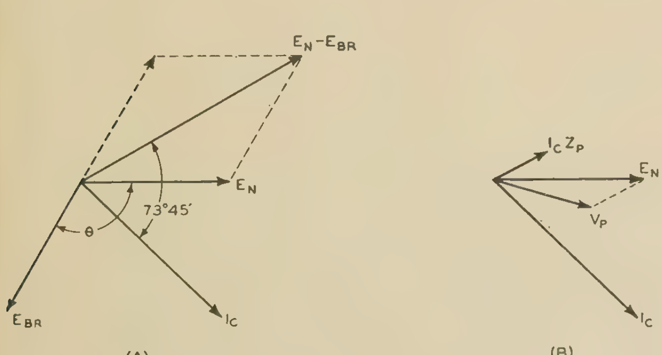


Figure 4. Vector relationship of voltages and current during out-of-step conditions

A—Circulating current I_c for displacement angle θ

B—Voltage at point *P* for circulating current I_c

voltages during the out-of-step condition. However, since the fault would normally be cleared by the time the actual pull-out occurred, and since the effect would be different for each fault location, it was neglected in the analysis. The effect for any special condition can be estimated fairly closely from the curves shown.

The following assumptions were therefore made:

1. The internal voltages of the generators at both stations remain constant and equal in magnitude.

2. The load current and line-charging current are negligible.

3. The effect of a fault during the out-of-step condition is neglected.

4. The unit impedances from the internal voltage, E_N, at Neches to the equivalent internal voltage E_{BR}, at Baton Rouge are as shown in figure 5*A*.

As the two systems swing apart, the difference voltage, $E_N - E_{BR}$, between the internal voltages at Neches and Baton Rouge will cause a circulating current $E_N - E_{BR}/Z$ to flow, where Z is the total impedance between E_N and E_{BR} including the generator transient reactances. The voltage and current will be balanced between phases so that only one phase need be considered.

The relation existing between the internal voltages, the difference voltage, $\boldsymbol{E_N - E_{BR}}$, and the circulating current $\boldsymbol{I_c}$, is shown vectorially in figure 4*A*. The circulating current $\boldsymbol{I_c}$, which is shown vectorially equal to $\boldsymbol{E_N - E_{BR}/Z}$, may also be expressed mathematically as,

$$I_C = \frac{E(1 - \cos\theta - j\sin\theta)}{Z} \quad (1)$$

where $\boldsymbol{E_N}$ and $\boldsymbol{E_{BR}}$ have the same magnitude E, and θ is the angle that $\boldsymbol{E_{BR}}$ lags $\boldsymbol{E_N}$. The angle of 73 degrees 45 minutes that $\boldsymbol{I_c}$ lags the difference voltage is the natural impedance angle of the system.

The current $\boldsymbol{I_c}$ will be common throughout the tie circuit and is the current at each relay location for any given displacement angle, θ. However, the voltage will vary with location and at any one point, P, the line-to-neutral voltage is

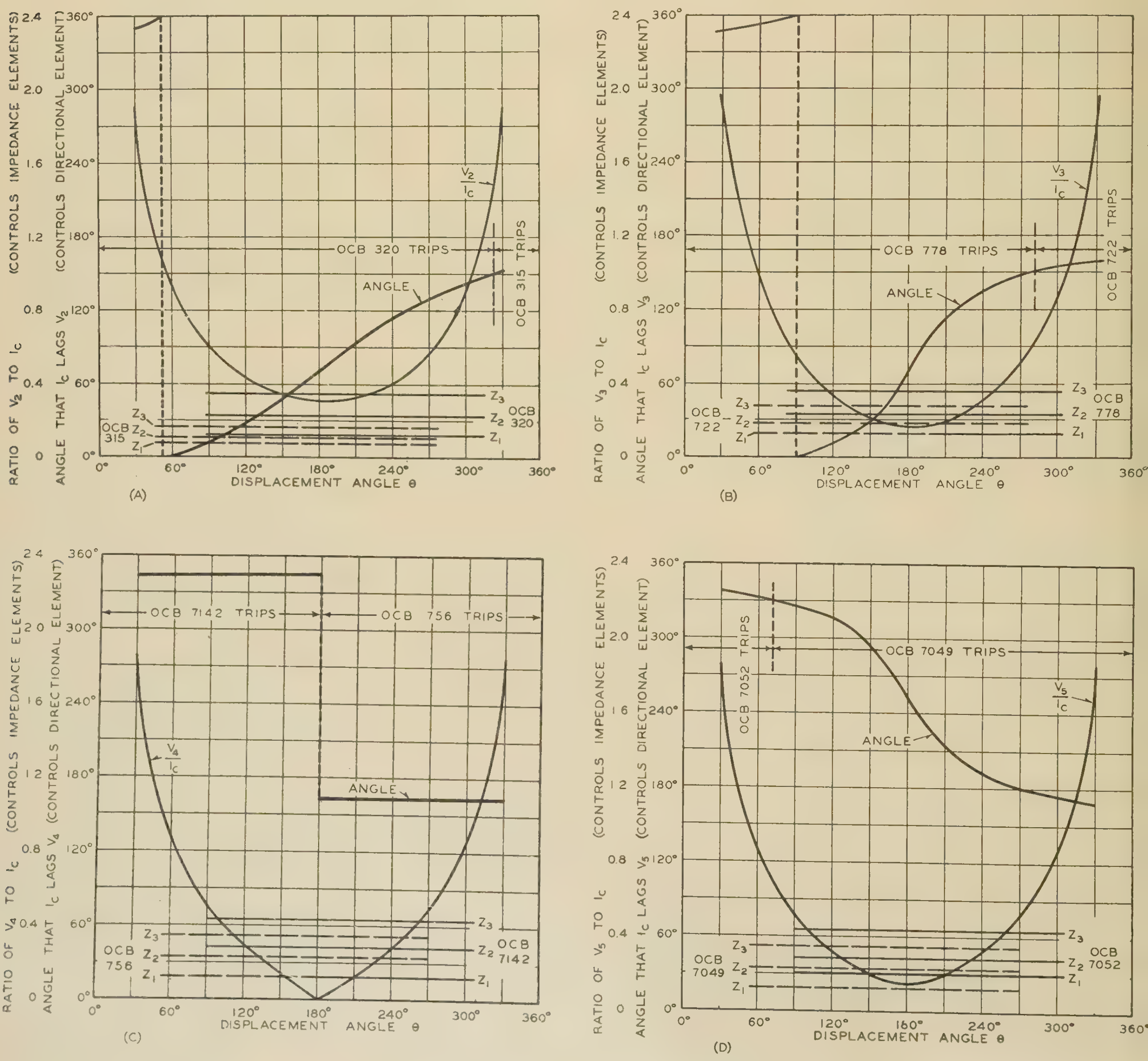


Figure 6. Ratio of voltage to current and operating zones of impedance relays at sectionalizing stations

A—Orange
B—Lake Charles
C—Jennings
D—Lafayette

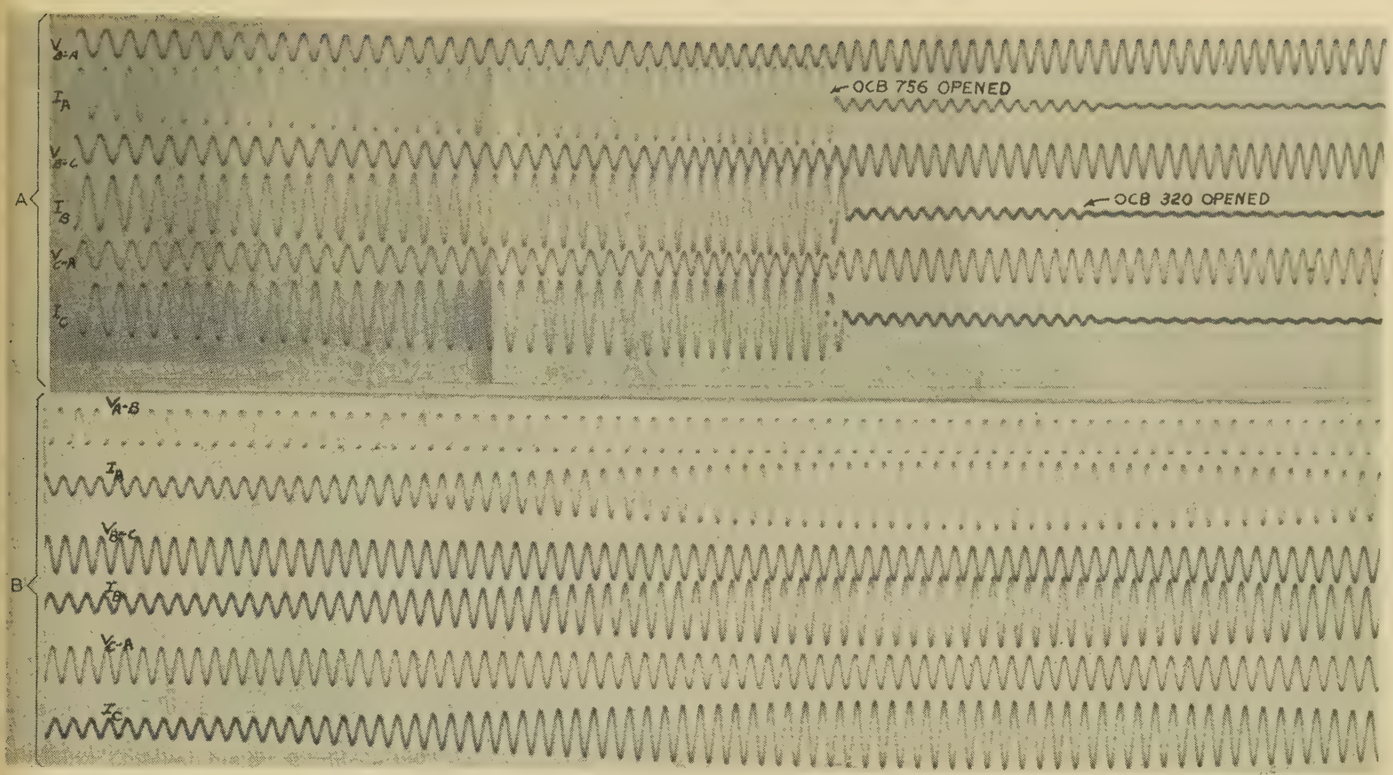


Figure 7. Typical oscillograms of voltage and current at Neches 66-kv bus during system disturbance

A—Out-of-step condition caused by disturbance on Baton Rouge end of system external to interconnections. Breakers 756, 778, 320 opened

B—Severe system oscillations caused by disturbance on Baton Rouge end of system external to interconnection. No breakers opened

equal to $\boldsymbol{E_N} - \boldsymbol{Z_P}$ where $\boldsymbol{Z_P}$ is the impedance from E_N to P. This relation is shown vectorially in figure 4B. It may also be expressed mathematically as,

$$\boldsymbol{V_P} = E\left[1 - \frac{\boldsymbol{Z_P}}{\boldsymbol{Z}}(1 - \cos\theta - j\sin\theta)\right] \quad (2)$$

The voltage and current at any point on the line for any displacement angle can be obtained either graphically as shown in figure 4, or analytically by means of equations 1 and 2. The loci of $\boldsymbol{I_c}$ and the line-to-neutral voltage vectors for a complete slip cycle are shown plotted in figure 5B. The vectors, shown for 120-degree displacement angle, represent unit values, where unit voltage is normal internal phase-to-neutral voltage, unit impedance is the total impedance between E_N and E_{BR}, and unit current is the current that would flow if $E_N - E_{BR}$ were equal to unit voltage.

It will be noted that the locus of the current vector, $\boldsymbol{I_c}$, is a circle with its center displaced one unit from the origin, that is, at $r - jX$, where r and X are the unit values of resistance and reactance corresponding to Z.

The loci of the voltage vectors will also be circles with their centers located at $1 - Z_P(r - jX)$ units from the origin. The phase angle and magnitude of the current or voltage at a sectionalizing point is equal to a vector drawn from the origin to a point on the respective circle corresponding to the displacement angle. The circulating current, $\boldsymbol{I_c}$, will increase to a maximum at 180 degrees displacement and back to zero as the two systems slip one pair of poles, that is, 360 electrical degrees, whereas the voltage decreases to a minimum at 180 degrees and back to a maximum at zero displacement.

It is evident from figure 5 that the voltages at the generating station 66-kv busses, V_1 and V_6, will not drop enough during out-of-step conditions to cause the relays at those locations to operate. The voltages will, however, drop appreciably at Orange, Lake Charles, Jennings, and Lafayette, particularly at Jennings which is almost exactly at the electrical center of the interconnection. The ratio of the

Switch Position	Switch A	Switch B	Operation
1	Open	Closed	Nonblocking
2	Open	Open	Out-of-step blocking
3	Open	Closed	Preferred out-of-step tripping

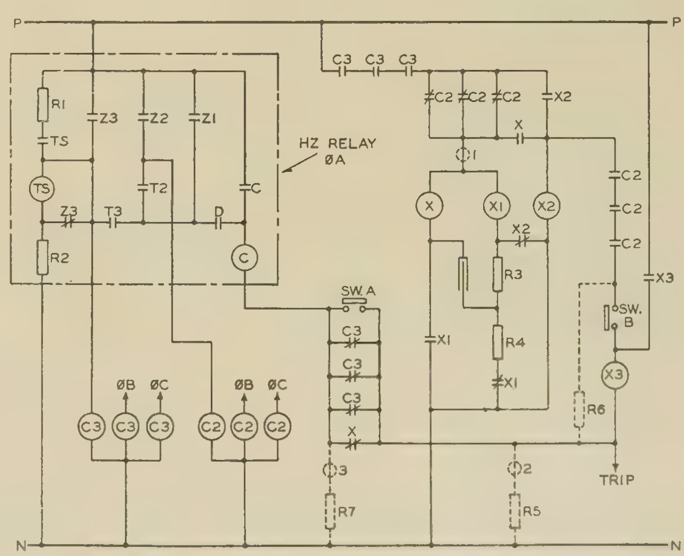


Figure 8. Schematic diagram of relay control circuit. Dotted connections show test connections of oscillograph galvanometers

voltage to current and the phase relation between them for these four points are therefore plotted against displacement angle, in figures 6*A*, *B*, *C*, and *D*. The actual settings of the impedance elements expressed as a ratio of voltage to current, and representing the balance points of the elements with the current lagging the voltage 60 degrees are also shown on the same curves. The shift in balance point with large changes in angle between the voltage and current is not shown, but if desired can be taken into account from available calibration curves on the *HZ* relay. From the curves of figure 6, it can thus be determined which breakers are subject to trip-out during an out-of-step condition.

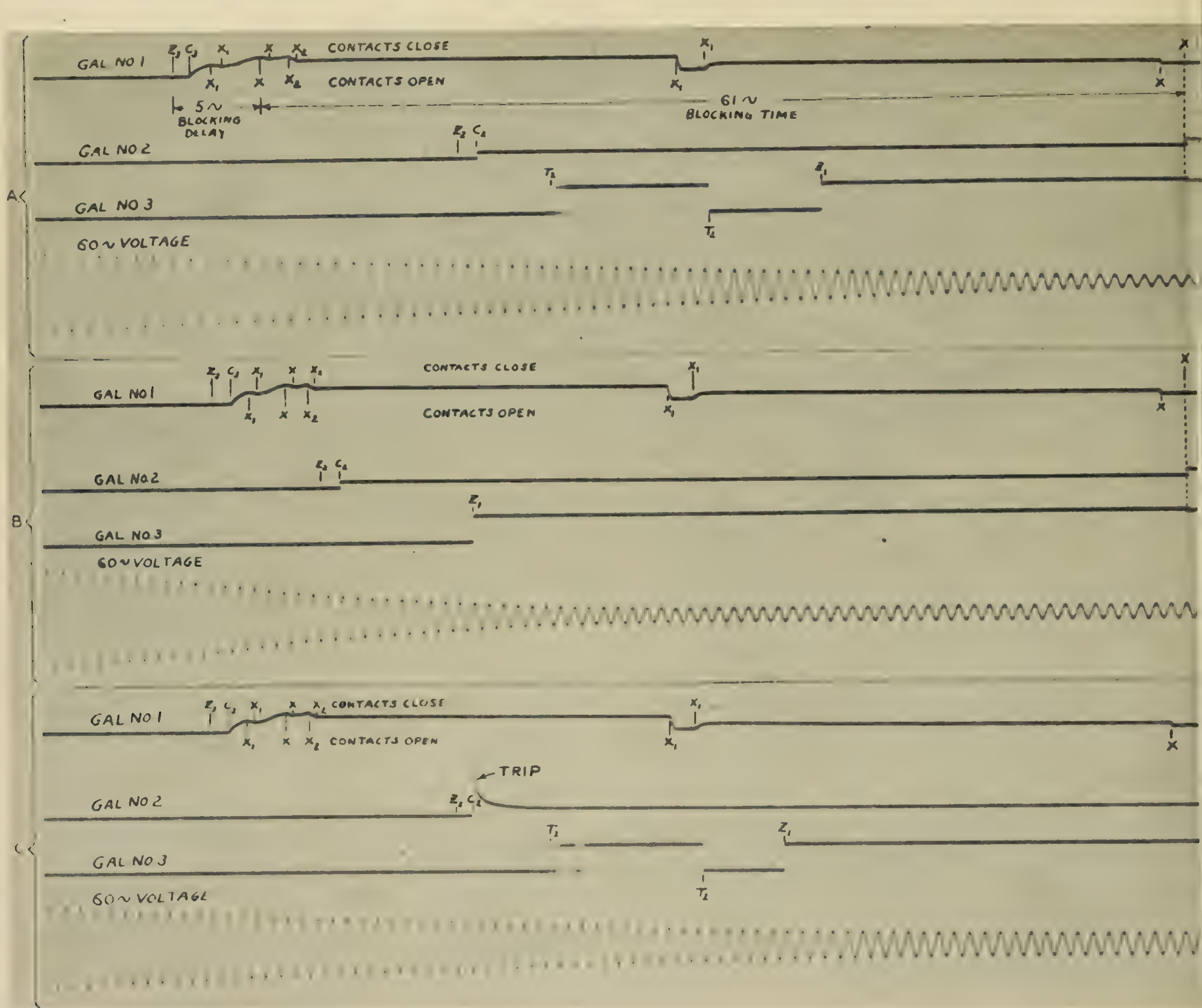


Figure 9. Oscillograms showing sequential operation of relays during simulated out-of-step conditions

A—Out-of-step blocking for slow pull-out

B—Out-of-step blocking for fast pull-out

C—Selective tripping during pull-out

Oscillograph Records

A number of oscillographic records of out-of-step conditions and system oscillations, occuring over several months' operation previous to the installation of the blocking relays, have been obtained by means of an automatic oscillograph located at Neches. The oscillograph, which was connected to record the 66-kv bus line-to-line voltage and the 66-kv tie-line currents, was initiated by an impedance-type relay. Although the oscillograms do not show the actual voltages at the intermediate sectionalizing points where the relays are most affected, they do show the circulating current I_c, and the voltage at Neches. These values can be interpreted from figure 5 and figure 6, to indicate the voltage and phase angles at the sectionalizing points.

Two typical examples are shown in figure 7. Oscillogram *A* shows the results of a pull-out due to a disturbance on the Baton Rouge end of the system, external to the interconnection which resulted in breakers 320, 778, and 756 opening. As shown by the oscillogram, at the time the oscillograph was initiated, the line voltage V_{B-A} was slightly lagging I_A, that is, I_A was lagging V_A by about 120 degrees, which from figure 5, indicates a displacement angle of 260 degrees. It will be noted from figure 6*C*, that at 260 degrees displacement, the Z_3 element of breaker 756 is nearly at its balance point. At the time breaker 756 opened, the current I_A was lagging V_A by about 90 degrees which indicates a displacement angle of 210 degrees, sufficient to cause tripping of the Z_2 element, or possibly Z_1 element.

This also shows that E_{BR} was advancing in phase position with respect to E_N, which might be expected with a disturbance near Baton Rouge. The Z_2 element of breaker 778, and the Z_3 element of breaker 320, both balance at about 220 degrees, and would therefore have closed their contacts shortly before 756 tripped. The total time shown on the oscillogram to the opening of breaker 756 is 39 cycles, which, allowing 15 cycles for breaker operation, would leave 24 cycles or just about the timing of Z_2 for breaker 756. Also, the Z_1 contacts for breaker 756 would have operated about that time, so that either the Z_2 contacts or the Z_1 contacts might have actually tripped the breaker. The timers for breaker 778 would have reached their second-zone contacts before breaker 756 opened and would therefore cause tripping on breaker 778. Breaker 320, which opened 15 cycles after breaker 756, was evidently tripped by its Z_3 contacts, just before 756 opened. Inspection of the relay that tripped breaker 320 showed that the Z_3 timer contacts had undoubtedly closed prematurely due to break contact on the Z_3 element being out of adjustment, which on the early design of *HZ* relay would energize the timer motor.

Oscillogram *B* shows oscillations between Neches and Baton Rouge, also due to a disturbance on the Baton Rouge end of the system. The disturbance in this case was not severe enough to cause the two systems to pull out of step. This oscillogram is of interest because it shows the natural period of oscillation.

Blocking-Relay Scheme

It is evident from the foregoing that the voltage and currents applied to the impedance relays during some part of a slip cycle may have the same relative values as for line faults located within the operating zones of the relays. It is also evident, however, that the ratio of voltage to current gradually decreases so that it comes within the zone of Z_3 first, then Z_2, and finally Z_1 if the relay is located near the electrical center. Relays located farther from the electrical center will close only their Z_3 and Z_2 contacts in succession. Also, since the out-of-step condition produces balanced voltage drops, the impedance elements of all three phases will operate in the order, $Z_3 - Z_2 - Z_1$ and then reset in the reverse order, that is, $Z_1 - Z_2 - Z_3$, to complete the slip cycle.

When a line fault occurs, the ratio of voltage to current at the relay reaches its final value almost instantly and remains substantially constant during the operating time of the relay. The basis of the blocking scheme is therefore the sequential closing of the impedance elements of all three phases during a slip cycle. The operation of the Z_3 elements of all three phases without the immediate operation of a Z_1 element causes the tripping circuit of the Z_1 and Z_2 elements to be blocked out long enough for the displacement angle to pass through the operating zones of Z_1 and Z_2.

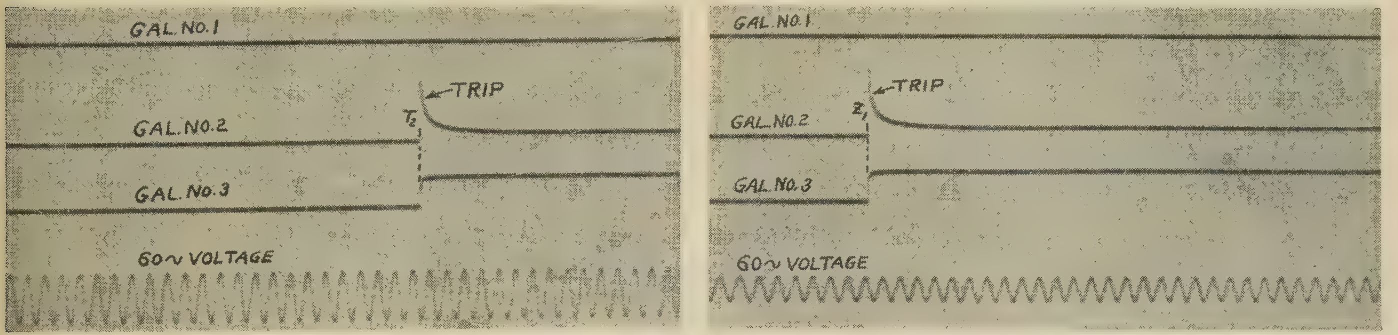
The blocking equipment, as shown schematically in figure 8, consists essentially of the three existing *HZ* impedance relays, two special type *SM* auxiliary relays, two type *SG* auxiliary relays, a capacitor, a resistor, and a control switch. The a-c connections to the *HZ* relays have been omitted from the diagram since they have no bearing on the blocking scheme which functions within the 125-volt d-c control circuit.

One of the *SM* relays consists of three instantaneous voltage elements, C_3, each with make and break contacts, and an instantaneous current element, X_3 with make contacts. The other *SM* relay consists of four instantaneous voltage elements, C_2 and X_2, each with make and break contacts. The two *SG* relays, *X* and X_1 are standard voltage relays used with the capacitor and resistor to provide time delay. The control switch is a three-position switch which can be set to obtain nonblocking, out-of-step blocking, or preferred out-of-step tripping.

Figure 10. Oscillograms showing operation of relays during simulated fault conditions

A (left)—Three-phase fault in zone of Z_2 and beyond zone of Z_1

B (right)—Three-phase fault in zone of Z_1



When out-of-step blocking is desired, the switch is set on position 2, so that contacts *A* and *B* are both open. As the systems pull out of step and the voltage-current ratio comes within the zone of the impedance relays, the three Z_3 contacts will close, thus energizing the three C_3 contactors which will open their break contacts connected in parallel in the trip circuit and close their make contacts which are all connected in series. The closing of the three C_3 make contacts will energize the *SG* relay, X_1, from positive through the C_3 contacts, the C_2 break contacts and the X_2 break contacts to negative. The capacitor will be partially charged through the *X* coil, but not enough to pick up *X*. The closing of the X_1 contacts will energize the other *SG* relay, *X*, which will open its break contact in the trip circuit. It will also close its make contact to energize contactor X_2. When the break contact of X_2 opens, the coil of *SG* relay, X_1, will be disconnected from negative. However, the charging current to the capacitor will delay its opening several cycles. When X_1 make contact opens, the *X* coil will be disconnected from negative but its drop-out will also be delayed several cycles due to the capacitor charging in the reverse direction. The capacitor-resistor arrangement shown in the diagram will provide approximately 5 cycles delay in the opening of the *X* break contact after Z_3 contacts close and will keep it open approximately 60 cycles, giving an over-all blocking time of approximately 65 cycles.

When the displacement angle reaches the zone of the Z_2 elements they will operate and energize the C_2 contactors which will open their break contacts and close their make contacts. However, with switch *B* open, the C_2 make contacts will have no effect and by that time

the X_2 make contact will be closed so that the opening of the C_2 break contacts will have no effect. Also, during the time the X break contact is open, the trip circuit will be open to Z_1 and the timing circuits of Z_2 and Z_3.

When a single line-to-line fault occurs in any one of the impedance zones, neither all three C_3 elements nor all three C_2 elements will pick up and the *HZ* relays will function in their normal manner. In case of a three-phase fault in the first or second impedance zone, the Z_3 elements will operate and pick up all three C_3 elements. However, the C_2 break contacts will open before relay X picks up and prevent any blocking action. If a three-phase fault occurs in the zone of Z_3, the blocking period will be over before the timing period of T_3 elapses and normal tripping will be obtained. If a fault occurs in a blocked section during the out-of-step period, tripping will be delayed until the end of the blocking period.

When preferred out-of-step tripping is desired, the control switch is set on position 3, which opens switch contacts A and elements without any respect to the directional element or timing of Z_2, so that the system disturbance is minimized. Fault tripping is obtained in the normal way.

When the switch is in the nonblocking position 1, the blocking contacts are shunted out by contacts A, and contacts B are open, so that the blocking relays have no effect on the operation of the *HZ* relays.

The dotted connections in figure 8 show the scheme that was used to test the timing and sequential operation of the combined relay equipment with an oscillograph. The number 1 galvanometer of the oscillograph recorded the closing of

In making the tests, actual out-of-step conditions were simulated as closely as possible. The current coils of the *HZ* relays were energized with a constant 60-cycle current of 8.5 amperes. By means of a variable-voltage test set, the voltage coils were energized with a 60-cycle voltage that could be varied at a rate corresponding to the change in voltage-current ratio during an out-of-step condition. This voltage which was recorded by the number 4 galvanometer was also used as a timing indication on the oscillograms.

Typical results of the tests are shown in figure 9 and figure 10, which show respectively the action of the relays during

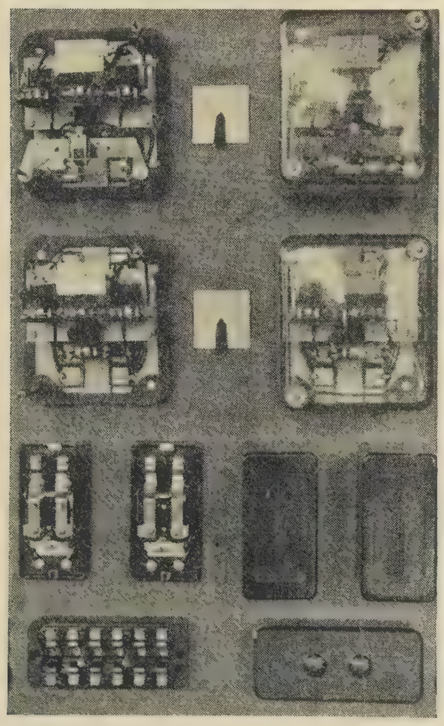
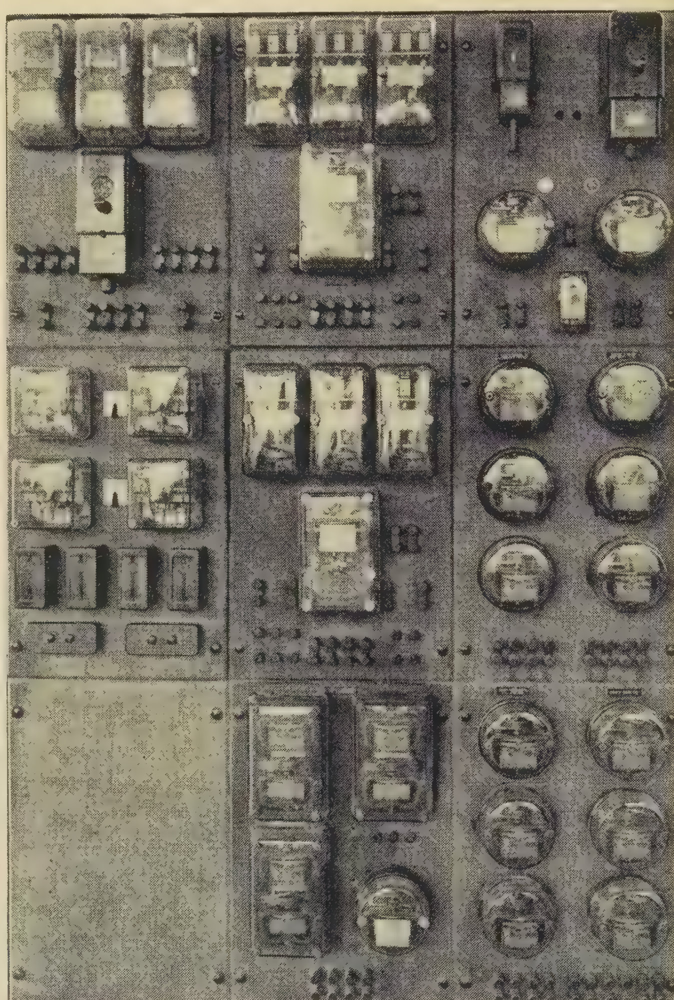


Figure 11. View of relay panels

A (left)—Two sets of auxiliary blocking relays with covers removed from one set

B (right)—Station switchboard with two sets of *HZ* impedance relays on middle panel and blocking relays on auxiliary panel to the left



closes switch contacts B. The operation of relays will be the same except that when the C_2 make contacts close the trip circuit will be completed through control switch contact B, and the breaker will be tripped without waiting for the directional contacts and either T_2 or Z_1 contacts to close. This is important as the interconnection is opened up at the desired point as soon as the out-of-step zone reaches the Z_2

the Z_3 contacts and the sequential operation of the X_1 and X relays. The number 2 galvanometer recorded the closing of the Z_2 contacts by measuring the current flowing through the resistor, R_6, in series with the resistor R_5. It also recorded when the trip circuit closed by the higher deflection caused by the current through R_5 only. The number 3 galvanometer recorded the operation of the Z_1 contacts.

out-of-step conditions and during three-phase faults within the operating zones of the impedance elements.

The oscillogram in figure 9*A*, which was taken with the control switch on position 2, for blocking, shows the sequential operation of the relays for a relatively slow slip cycle. It will be noted that a delay of five cycles is obtained between the closing of the Z_3 contacts and the

opening of the X break contact in the trip circuit. This is to allow time for the Z_1 or Z_2 contacts to close in case of a three-phase fault within the section and thus not delay normal tripping. The blocking period from the opening of the X break contacts until it closes again is recorded as slightly less than 61 cycles. It will be noted that the number 3 galvanometer recorded the closing of the T_2 timer contacts which occurred before the Z_1 contacts closed.

The oscillogram in figure 9B was taken under the same conditions as figure 9A, except that the 60-cycle voltage was decreased faster to represent a rapid pull-out. In this case the Z_1 contacts closed before the T_2 contacts.

The oscillogram in figure 9C was taken with the control switch in position 3, for preferred out-of-step tripping. As shown by the number 2 galvanometer tracing, the trip circuit closed immediately after the Z_2 contacts picked up and before the T_2 or Z_1 contacts closed.

The oscillograms in figure 10 were taken with the control switch in position 2, for blocking. In A, a voltage was applied to the impedance elements corresponding to a three-phase fault in the second zone. In B, the voltage applied was equivalent to a three-phase fault in the zone of Z_1. Both oscillograms show that the blocking relays did not pick up, thus permitting the impedance elements to trip in their normal manner.

The complete installation consists of eight sets of blocking equipment, that is, two each at Orange, Lake Charles, Jennings, and Lafayette. The only change necessary to the HZ relays was to modernize them by providing two additional studs and a switch-controlled timer as shown in figure 8. A view of two sets of the auxiliary blocking relays mounted on an auxiliary panel section is shown in figure 11A. The covers have been removed from the set on the left to show more clearly the different elements of the relays. The selector switches are located between the two sets of relays, as shown. The capacitors and resistors are mounted on the back of the panel. Figure 11B shows a view of a complete installation at one station consisting of two sets of HZ relays on the two upper sections of the middle panel, and the two sets of auxiliary relays on the middle section of the left-hand panel.

Conclusion

The method used for calculating line voltages and currents provides a means of determining the performance of distance relays during out-of-step conditions. The automatic oscillograph is also invaluable in studying voltage and current characteristics during the slip cycle.

Out-of-step blocking of high-speed impedance and reactance relays has been available where carrier current or pilot wires are used as a medium of control. The scheme described in this paper can be readily applied to existing installations of impedance relays or used with new installations where carrier current or pilot wires are not available. In addition to blocking, the scheme provides a means of rapid tripping during out-of-step conditions at any desired location which can be selected by means of a selector switch. At the time of writing this paper the equipment was just being installed and had not been put in operation. However, the rigid field tests that were made with simulated out-of-step conditions indicate that the scheme will function as intended.

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Discussion

E. H. Bancker (General Electric Company, Schenectady, N. Y.): For some five or six years prior to 1934 there were many studies of stability in which the effect of relays upon system stability was an important factor. In reference 3 Mr. Hunter and the writer undertook to study the reverse of this situation and to find out what effect instability had upon relays. This was further explored by Mr. Mason in his paper "Relay Operation During System Oscillations," which appeared in volume 56, July 1937, Electrical Engineering, page 823. Reference 2 outlines a study of a specific system from both points of view.

During the course of the investigation of possible means for preventing incorrect relay operation during instability, a scheme was devised which is mentioned briefly on page 194 of reference 2. This scheme is covered in more detail in United States Patent No. 2,030,665. This arrangement is intended particularly for application to reactance-type distance relays and consists in changing the characteristics of the starting unit after the started unit has operated. It prevents relay operation during a system swing when conditions are similar to those existing during a fault, and yet permits relay operation under actual fault conditions.

This paper presents an interesting application to distance relays alone of the principles now in use as a result of these earlier studies for blocking tripping of carrier pilot relaying during system oscillations.

In June 1936 there was published a French patent No. 799,279 which disclosed an arrangement for preventing unwanted operation of impedance relays by utilizing the progressive drop in indicated impedance during a slip cycle in contrast with the immediate drop that occurs when a fault starts.

This paper is an interesting sequel to these others in giving the details of another actual installation and its characteristics during loss of synchronism and in giving in detail the application of the principles now in use, and those outlined in the French patent. Of special interest is the oscillographic record of actual cases of loss of synchronism and bad system oscillations. Such records, in conjunction with the mathematical analysis, make possible still more refinement in designing and selecting protective apparatus in such a way as to increase service continuity.

In view of the very complete research which has already been made and described regarding relay operation during system oscillations not accompanied by a fault on the system, it is regrettable that the authors have neglected to analyze the possible effects of the presence of such a fault during the oscillations. It is fairly obvious that with such a fault, all three phases will not be similarly affected. It is very likely that the Z_3 impedance element of one phase will operate in a much different fashion from the Z_3 elements in the other two phases. How this will affect the functioning of the scheme described by the authors, wherein the simultaneous operation of all Z_3 elements has been assumed, should be analyzed carefully to prove its dependability under all practical operating conditions. It is to be hoped that a sequel to this paper will appear with the oscillographic evidence to indicate the success attained.

H. R. Vaughan: Mr. Bancker's discussion emphasizes the statements made in the opening paragraph of the paper regarding the previous recognition of out-of-step phenomena and the generally accepted conclusion that carrier current was required to prevent out-of-step tripping where high-speed relays were involved.

Even before 1934, the effects of instability on distance-type relays were quite well recognized. In 1932, I had an opportunity to assist in investigating on the network analyzer, the instability influences on distance-type relays in operation on a specific system. The results of the investigation

showed conclusively that certain conditions of instability would cause the relays to operate and accounted for questionable relay operations that had been experienced. This condition was overcome by speeding up other relays to prevent instability. The paper that Mr. Bancker and Mr. Hunter presented in 1934 as well as the later papers referred to are valuable contributions toward a better understanding of the effects of out-of-step conditions on distance-type relays.

Mr. Bancker refers to a scheme intended particularly for reactance relays that was mentioned briefly on page 194 of reference 2 and covered in more detail in United States Patent No. 2,030,665. It is stated on page 194 of reference 2 that the scheme appeared doubtful and that carrier-current control was adopted instead, thus bearing out the statement in our paper that carrier current was considered necessary to provide out-of-step blocking.

Regarding the effects of the presence of a fault during system oscillations, it is recognized that the majority of pull-outs are caused by faults. However, as stated in the paper, the fault would normally be cleared by the time actual pull-out to the point of relay tripping occurred, so that the operation of the blocking relays would not be appreciably affected. This is borne out by the automatic oscillograms, two of which are shown in the paper. The voltages and currents shown by these oscillograms which were taken during pull-outs caused by system faults external to the interconnection, check closely the values calculated by neglecting the effects of the fault.

The effects of long-duration faults external to the interconnection could be readily determined by means of the a-c network analyzer, or more laboriously by calculations. In general, the electrical center of the interconnection would be shifted toward the fault. If the fault were phase to phase, the fault current fed through the interconnection might cause the Z_3 element of the relay associated with the faulted phases to pick up. However, the generators nearest the fault would normally advance in phase angle so that the circulating current due to the out-of-step condition would counteract the current supplied through the interconnection to the fault. This would tend to equalize the impedances indicated to the three relays so that all three Z_3 elements should pick up before any one relay could cause tripping.

The blocking scheme is based on all three Z_3 elements being in the operated position at one time, but it is not necessary for them to pick up simultaneously. The pick up of one or two Z_2 elements before all three Z_3 elements picked up would not prevent blocking. It is only necessary that all three Z_3 elements be in the operated positions before one of the Z_1 contacts and its associated directional contact close simultaneously, or before one of the Z_2 contacts and its associated timer and directional contacts close at the same time. It is believed, therefore, that the blocking relays will function properly even though an external fault exists at the time of an out-of-step condition. As operating experience and additional oscillographic data are collected it may be necessary to alter some of the Z_2 and Z_3 settings or to change the timing periods of the blocking relays.

Improvements in the Construction of Condenser Bushings

A. J. A. PETERSON
MEMBER AIEE

Synopsis: Condenser bushings use paper to provide high dielectric and physical strength. The value of paper as an insulation is recognized by its accepted use as the insulating medium in cables, transformers, and other apparatus. It is also recognized that unlimited life is attained when proper protection is provided against external influences. Such protection is obtained by improved methods of winding the capacitors, treatment with oil impregnation, and surface varnish. Further protection is provided for outdoor bushings by sealing the condenser in a weather casing of porcelain with improved flexible caps and gaskets. Insulating oil or a plastic heavier than water is available for encasing the capacitor.

MANY thousands of transformers and circuit breakers installed during the past 30 years have been equipped with condenser-type bushings. This type of construction has been used here and abroad because first, the distribution of the voltage stresses through the bushing is such as to lower the concentration of such stresses and thereby increase the resistance of the bushing to dielectric breakdown, and second, it has great mechanical strength and resiliency. The condenser bushing is composed of paper and an organic bond. Here, as in transformers, capacitors, cables, and other high-voltage apparatus, paper provides the high mechanical strength and high electrical, especially impulse strength. In winding the bushing the paper is divided into a number of condensers by the insertion of foil layers at intervals selected to provide the proper distribution of the voltage stresses. These well-known principles have been in use since the first condenser bushings were manufactured.

It is recognized that the life of paper as an insulating medium is without limit as long as it is retained in its original state. The greatest enemy to the life and dielectric strength of such insulation is moisture. The manufacturer and the user have both aimed for such improvements as would definitely retain the original characteristics of the paper. Recent improvements in the construction of condenser bushings have markedly increased their resistance to the effects of moisture.

In a condenser bushing, the condenser—or core, as it is sometimes called—is the insulation member. The first step in improving the moisture-resistant characteristics of the bushing is to make the condenser tight and homogeneous. This was found to be a matter of processing, requiring a definite proportionment of bonding material to the weight of the paper, as well as a more rigid control of the relative speed, pressure, temperature, and tension during the winding process. That such tight bushings can be made is indicated by the fact that in

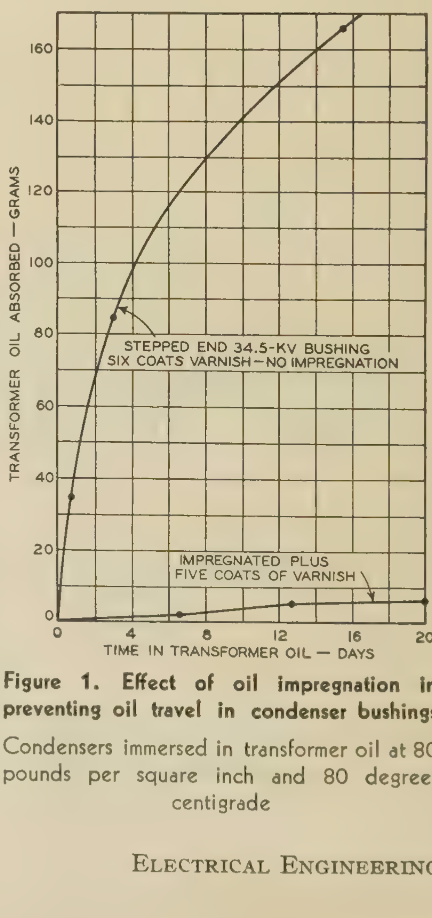

Figure 1. Effect of oil impregnation in preventing oil travel in condenser bushings

Condensers immersed in transformer oil at 80 pounds per square inch and 80 degrees centigrade

Paper number 39-88, recommended by the AIEE committee on protective devices, and presented at the AIEE South West District meeting, Houston, Tex., April 17–19, 1939. Manuscript submitted March 31, 1938; made available for preprinting March 15, 1939.

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The writer gratefully acknowledges the assistance of H. J. Lingal in preparing this paper and of C. F. Hill, J. G. Ford, and E. E. Arnold for the tests and data responsible for these developments.

commercial production, bushings must withstand a pressure test of 60 pounds per square inch without leaking.

A polymerizing organic oil is now used to impregnate the condenser to considerable depth in order to increase its resistance to moisture. The condenser is then cured by an appropriate time and temperature process, which converts the oil from a liquid into a permanent solid by a dual process involving both oxidation and polymerization. This treatment also increases the effectiveness of the varnish by providing for it an improved base.

The oil used for impregnating the condenser was selected after an exhaustive investigation of many materials. The choice was made on the basis of its power of penetration, ease of curing, electrical characteristics, and effectiveness as a seal against both oil and water. Comparative data were obtained by testing sample 34.5-kv bushings with various impregnating materials, times of curing, and other processing details. These data would be interesting from the viewpoint of chemistry or physics, but for the present purpose it should be sufficient to say that the oil now in use was found to be superior in the essential features.

The effectiveness of the impregnating oil treatment on sealing the pores of the condenser is shown by figure 1. The data for these curves were obtained by immersing duplicate bushings in transformer oil at 80 degrees centigrade, with an applied pressure of 80 pounds per square inch, for 19 days. The weights of the bushings were measured at intervals in order to determine the progressive

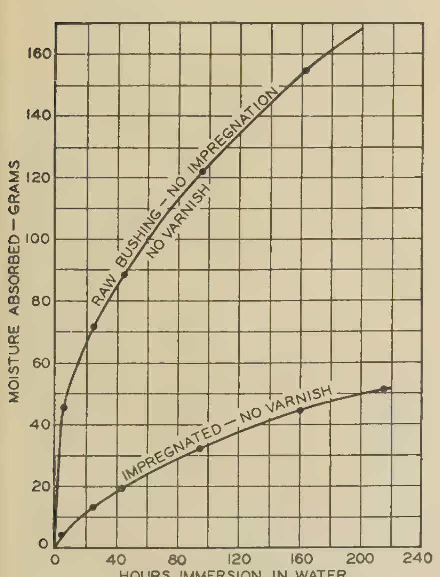

Figure 2. Effect of oil impregnation on rate of moisture absorption; 34.5-kv condensers stepped on one end, immersed in water

absorption of the hot oil. The oil-absorption capacity of the untreated bushing was determined by test and calculation to be slightly greater than 400 grams, so the absorption of approximately 6 grams by the treated bushing after 19 days must be regarded as negligible. This test is very exaggerated and is no measure of the absorption of the bushing under ordinary operating conditions, but serves under accelerated conditions to demonstrate the relative improvement obtained by the new oil-impregnating treatment.

Similar tests were performed to determine the effect of the oil impregnation on the absorption of free moisture. Sample bushings were immersed in water, and weighed at intervals to observe the progressive absorption. Of the various tests

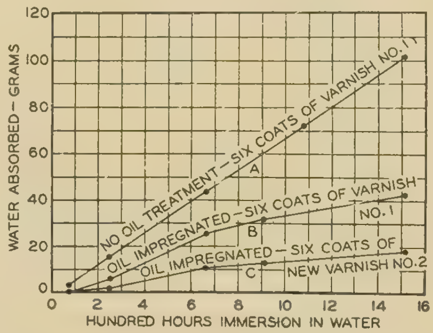

Figure 3. Effect of improved varnish blend and effect of oil impregnation as measured by moisture absorption; 34.5-kv bushings stepped on one end, immersed in water

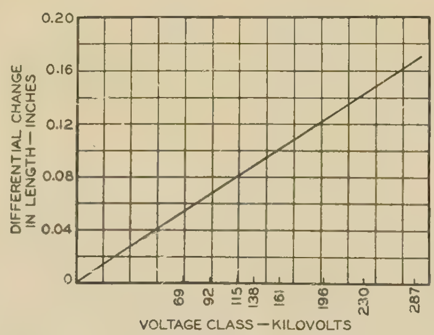

Figure 4. Approximate change in length between condenser core and porcelain casing of bushings of various voltages for 100 degrees centigrade change in temperature

performed, data showing the relative performance of the impregnated, unvarnished condenser and that of the raw condenser, untreated, were selected for figure 2. In the untreated condenser the moisture is absorbed rather rapidly at first, and then at a decreasing rate. The treated condenser shows a marked reduction in the initial rate of absorption and further slowing up of the absorption process after a few days. This curve indicates that after 19 days total immersion in water the absorption by the condenser was only about one-eighth of its theoretical capacity of approximately 400 grams.

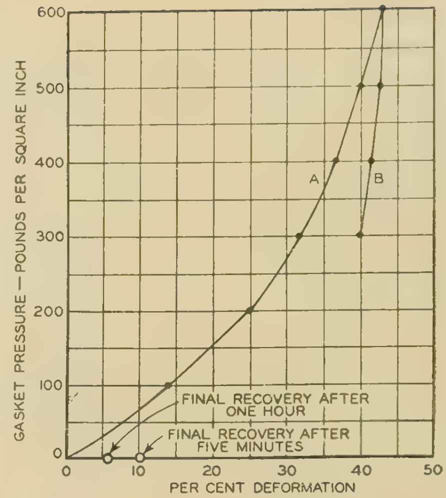

Figure 5. Deformation test on cork-Neoprene gasket material showing compression and return toward original dimension

Another interesting accelerated test was performed by testing bushings under oil at pressures of 800 pounds per square inch. After two hours of such treatment the bushing with only the varnish treatment absorbed ten grams of oil, whereas the oil absorbed by the impregnated bushing was too small to measure.

Obviously, such severe tests serve only to determine and illustrate the efficiency of the impregnating oil treatment in resisting moisture penetration of the condenser, and reflect actual operating conditions only in a greatly exaggerated way.

After the impregnating oil had been selected, research was directed toward obtaining a superior varnish treatment for the surface. The following are some of the more important characteristics which were sought:

(*a*). High resistance to electrical leakage over the surface.

(*b*). Resist the tendency to "tree" or "track" under high potential stress.

(*c*). Seal the bushing against the penetration of moisture, oil, ordinary acid fumes, etc.

(*d*). Resist abrasion such as is met with in normal handling.

(*e*). Provide a hard, smooth surface to avoid holding of dust, carbon and other foreign substances and to facilitate cleaning.

(*f*). It must not crack or craze from age or temperature changes.

Many varnishes show good oil-resistance characteristics, so work was con-

centrated on finding one having the desired moisture resistance along with the other required qualities. Sample bushings were treated with various varnishes and varnish combinations and then exposed to tests similar to those used in selecting the type of impregnating oil. Selection was based on resistance to moisture absorption by the condenser as measured by weighing, and the ability to withstand various tests and accelerated weathering tests under varying temperature and humidity conditions and ultraviolet exposure. Of course, the varnish must be suitable for ordinary processing. As no commonly known varnish was found to provide all the desirable features, a blend was compounded of materials having individual superiorities, so that the new varnish shows superiority in all requirements. The results of the tests on water absorption are shown in figure 3. Curve *B* shows by comparison with curve *A*, the improvement obtained by adding the oil impregnating treatment to the old varnish treatment. Curve *C* shows the further superiority of the new varnish treatment over the old. The moisture absorption is reduced to about 40 per cent of that in older bushings, by the impregnating oil treatment, and further reduced to less than 20 per cent by using the new varnish blend. An interesting feature of these tests is that even after 63 days total immersion in water, the power factor of the bushing with the improved impregnating and varnish treatment was only 3.36 per cent—still an operative bushing.

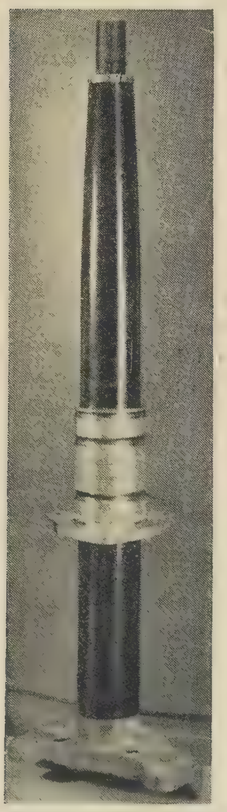

Figure 6. A 23-kv condenser bushing—indoor service

Figure 7. A 69-kv condenser bushing—outdoor service

Treeing, tracking, and crow-footing are terms which have been applied to the phenomenon of creepage occurring on insulating surfaces when the latter are subjected to abnormal electrical stresses. It appears to be initiated by a leakage current along the surface with local concentrations of heat, resulting in gradual thermal breakdown or carbonization of the surface and possibly resulting in the formation of a conducting path. The American Society for Testing Materials test has been found most reliable for determining the arc-resistant characteristics. The resistance to tracking is determined by measuring the time in seconds to form a conducting carbon path on a dry surface when a small, low-energy arc plays continuously across the surface between two point electrodes one-half inch apart, resting on the surface. The varnish selected has an arc resistance equal to the best of the materials applicable as a varnish.

The three processes just described namely, tightness, oil impregnation, and varnish treatment, applied to indoor bushings, provide the moisture-resistance characteristics necessary for the most humid atmospheres. For outdoor bushings, it is desirable to add a porcelain weather casing to the exposed end of the bushing, clamping it down on the bushing flange by a suitable expansion cap and gaskets.

It would be a relatively simple matter to encase the condenser in a porcelain if it were not for the changes in temperature met with in service. The coefficient of expansion of the condenser is greater than that of the porcelain. With a rigid cap coupling between the condenser conductor and the porcelain this difference would be enough to release the pressure on the gaskets at extremely high temperatures.

Figure 4 indicates the differential expansion for bushings of various service voltages. This difference in expansion is taken care of by providing a flexible coupling between the condenser conductor and the porcelain. In the simplest form this resiliency is provided in the cap by shaping it so as to supply the necessary spring action over the range of temperature. In other assemblies the spring element is provided by a separate spring or group of springs enclosed within the

Figure 8. A 138-kv condenser after oil impregnating treatment

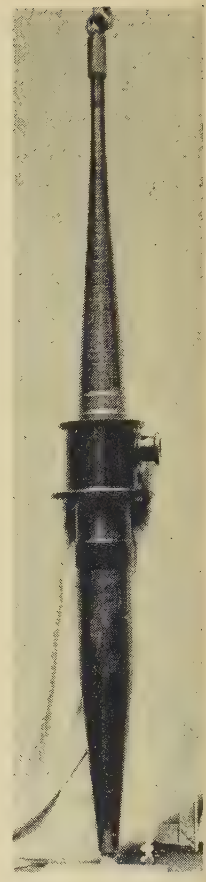

Figure 9. Same as figure 8 except cleaning and applying the varnish

cap. With any type of cap it is necessary to design for sufficient length of spring action so that at the lowest temperatures the pressure on the assembly is not excessive, while at the highest temperature, when the differential expansion is the greatest, there remains sufficient pressure to maintain the rigidity of the assembly and the efficiency of the seal.

Considerable advance has been made in the materials and designs of gaskets. The mixture of ground cork and Neoprene has been found to provide satisfactory characteristics of oil and moisture resistance, flexibility, and long life. It has also been found that the best life of the gasket is obtained by compressing the gasket to about two-thirds of its original thickness, limiting this compression by a so-called "gasket stop." The latter may take the form of an auxiliary gasket of harder material, or may be provided by a ridge on the gasketed surface.

The best performance is obtained by limiting the maximum pressure on the gaskets to about 300 pounds per square inch. This has been determined by applying various loads and measuring the ability of the gasket to return to the original dimension.

Figure 10. Same as figure 8, completely assembled with weather casing and cap

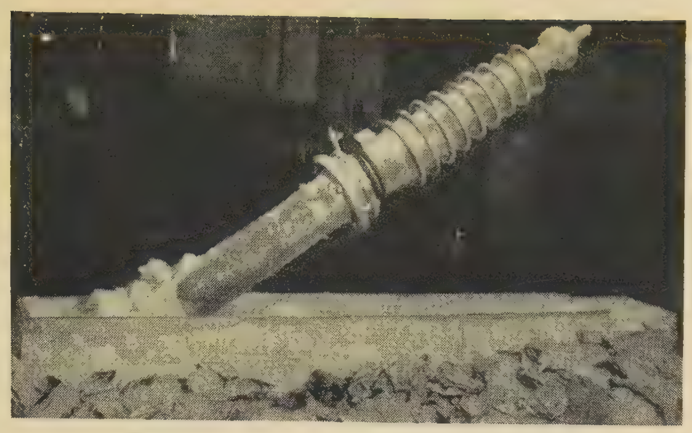

Figure 11 (above). Outdoor condenser bushing assembly being removed from dry-ice bath during temperature - change test

Figure 5 shows the deformation characteristics of gaskets made from ground cork and Neoprene. The unlimited compression of sample gaskets was measured under various loadings up to 600 pounds per square inch. The return curve *B* was obtained by releasing the pressures, taking readings down to 300 pounds per square inch. Five minutes after pressure was completely released the gasket had returned to within ten per cent of its original thickness and one hour after pressure was released the gasket had returned to within five per cent. It is well known, of course, that with lower pressures, the return toward the original dimensions becomes greater. The pressure of 300 pounds corresponds to about one-third compression, which determines the relative size of gasket stop. The area of the gasket is, therefore, designed to obtain a maximum pressure under lowest temperature and full compression of the spring of approximately 300 pounds per square inch. The data shown in figure 5 illustrate the ability of the gasket to withstand abuse. However, the use of a gasket stop and spring pressure maintained from the cap insures that the gasket is kept at a constant dimension and under a constant pressure. This eliminates any movement of the gasket under temperature change and so contributes to its long life and high efficiency as a seal.

Insulating oil and plastic materials are used for filling the space between the condenser and the porcelain weather casing. A new plastic material heavier than water and highly moisture resistant has been developed for this purpose. In addition to the high specific gravity, other requirements must be met by the material in order to make it suitable for use in bushings. Among the more important are:

1. Specific gravity greater than water throughout the temperature range
2. High dielectric strength
3. Low moisture absorption
4. Not be too fluid at operating temperatures
5. Be plastic at low temperatures
6. Good adhesion to both the condenser and the porcelain
7. Sufficient fluidity to permit easy filling of the bushing

In addition, the material must be stable and neutral in its effect on condenser finishes, gasket materials, and metal parts.

The new plastic material, which has these characteristics, has been tested in the laboratory, and in outdoor service in bushings and with moisture present. In the laboratory tests, 34.5-kv bushings have been operated for over a year with water on top of the filling material, with 70 degrees centigrade flange temperature to soften the compound and at 30 kv. No change was detected in the power factor of the bushing, showing no moisture absorption. In addition, the material itself does not absorb any measurable quantity of moisture.

This material is quite plastic at temperatures as low as −40 degrees Fahrenheit, and will not pull away from the condenser or the porcelain until the temperature has gone down to below −60 degrees Fahrenheit. The material remains sufficiently viscous at 70 degrees centigrade to prevent convection currents, but still can be poured at a temperature of 100 to 125 degrees centigrade.

In addition to the laboratory tests, a large number of bushings using this new filling material have been exposed to the weather for almost a year, operating at a voltage corresponding to the line-to-line voltage instead of line-to-ground voltage. In some of these the caps have been opened up to permit free entrance of moisture to the top of the bushing and some had water introduced on top of the compound at the beginning of the test. Others were sealed up in the conventional manner. This continued operation under higher-than-normal voltage confirms all the laboratory tests, insuring that moisture entering accidentally will be retained in a harmless location at the top

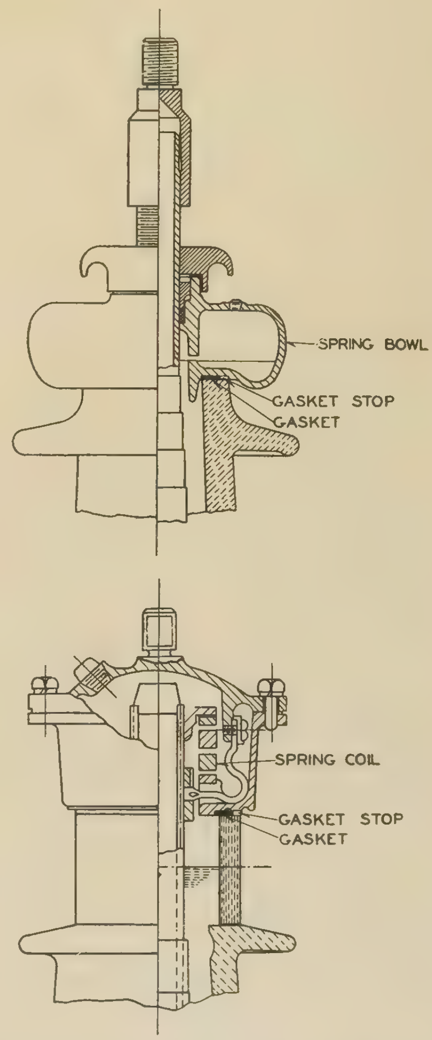

Figure 12. Types of caps showing spring action to maintain rigidity of the assembly and effectiveness of gasket seal

of the bushing and will not get in contact with the condenser itself.

A conventional 115-kv oil-encased bushing with a weather casing containing ten quarts of oil and two quarts of water was operated continuously with 132 kv, twice normal voltage to ground, applied between the bushing terminal and the grounded flange. The bushing stood this test with no measurable change in power factor, so the test was concluded after 60 days.

The primary purpose of these developments was to produce a condenser core which would resist the penetration of moisture. This has been obtained by means of materials, processes, and surface finish. The second step in the development, applicable to outdoor bushings, was to design an assembly which would be effectually sealed against the entrance of moisture to the condenser. This is a matter of proper porcelain, gasket, and cap design. With these conditions fulfilled, it matters little whether the space surrounding the condenser is filled with oil or compounds as this is not required for insulation. As a matter of interest, we have been told of a situation in which a whole set of outdoor bushings is operating with nothing but air filling the space between the condenser and the porcelain, using a heater at the flange to keep the air dry.

Discussion

L. Wetherill (General Electric Company, Pittsfield, Mass.): Mr. Peterson makes the statement that moisture is the greatest enemy to the life of paper insulation. The facts involved might be more accurately represented by stating that moisture has an adverse effect only in cases where the insulation is not adequately protected by suitable long-lived gaskets. It is never desirable to operate a bushing with defective or inadequate gaskets, or with a defective porcelain.

While occasional cases of fractured porcelain can probably never be eliminated, the new gasket materials which have become available in the last ten years offer an effective means of providing effective and permanent protection from moisture. It has been possible to eliminate troubles on

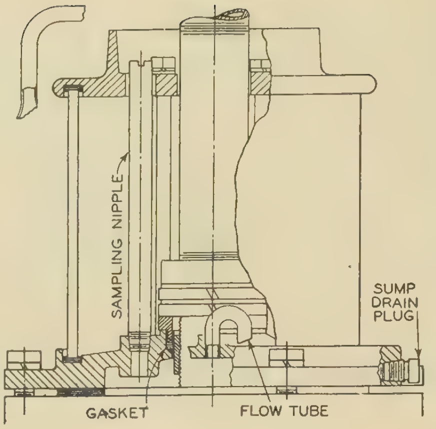

Figure 1. Thermal seal

the older bushings in service by regasketing. Experience over the last ten years, involving hundreds of thousands of gaskets, strongly indicates that gasketing is no longer a major problem on bushings of modern design.

The experience of the writer agrees with that of Mr. Peterson in that proper control over the materials and conditions during the winding of bushing cores will give a solid and impervious structure. It is the practice of the General Electric Company to require that solid bushing cores must withstand a gas-pressure test of 80 pounds per square inch applied for ten minutes. Experience has shown that a test of this severity assures high quality and long life in service.

There is one additional phase of the differential thermal expansion problem not mentioned by Mr. Peterson. In bushings using solid cores, it is the practice of the General Electric Company to use insulation with coefficient of thermal expansion matched with that of the copper conductor, in order to eliminate internal strains in the core. Such procedure means that a core which is initially tight will remain tight in service.

Mr. Peterson has found that the use of 300 pounds per square inch maximum gasket pressure and about 33 per cent gasket compression give best results. These conclusions are influenced doubtless by the composition and configuration of the gaskets involved. For example, tests on a somewhat harder mixture of cork and Neoprene show that the pressure required for 17 per cent compression varies from 1,100 to 1,900 pounds per square inch, depending upon the dimensions of the gasket.

The trend toward the use of fluid filling compounds in higher voltages is apparently continuing. Fluid compounds do not form shrink voids or show cumulative deterioration as a result of occasional local overstress. They also avoid the danger formerly resulting from the use of solid filling compounds which permitted defective gaskets to remain in service unrecognized.

On the larger oil-filled bushings the General Electric Company, for nearly three years, has been using an open ventilated construction but with the oil protected from the atmosphere by a thermal trap similar in its effect to the conservator used on many power transformers. This construction is shown in figure 1 of this discussion.

The inverted U tube, which connects the bushing with the expansion chamber, serves to prevent circulation of oil by convection currents; and the interchange of oil between the bushing and the expansion chamber is limited to the small amount necessitated by volumetric thermal expansion of the oil. Extensive accelerated tests have shown that bushings utilizing this construction retain their original dielectric condition permanently.

A. J. A. Peterson: Mr. Wetherill's discussion emphasizes, in an interesting manner, the approach of another manufacturer to the problem of protecting the bushing so as to give it longer life. Testing pressures, operating stresses on gaskets, and so forth are matters of individual and detail design. It is interesting to note, however, that the sufficiency of pressure testing to determine sound cores is influenced by the time during which such pressures are applied. Long experience in manufacturing and testing condenser bushings has led to the adoption of combinations of pressure and time to insure the most satisfactory results.

As pointed out in the paper, the numerous improvements in condenser bushings have made the same condenser core suitable for either fluid or plastic encasing material. Both oil and plastic as used in condenser bushings eliminate shrink voids referred to by Mr. Wetherill, and the condenser principle inherently avoids the local overstresses which might be present in other designs.

Gaskets serve not only to keep moisture out of the bushing but also to keep the filling material in the bushing. This is of major importance in bushings where the oil contributes or forms the main part of the insulation, and is obviously of less importance in the capacitor bushing.

There is likewise considerable difference in the relative importance of the filling material of various types of bushings. The condenser unit of a Westinghouse bushing is the real insulating medium and the oil or plastic serves only to protect the condenser, and does not contribute materially to the dielectric strength of the bushing.

Dielectric Strength of Porcelain

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FUNDAMENTALLY, the voltage-time characteristic of insulation suggests the extending of studies for fullerboard and transformer oil[1,2] to other insulation materials. Porcelain is considered in this investigation since it is used extensively for line and substation insulation, in apparatus design as bushings,[3] and for other insulation.

The determination of the impulse characteristics of porcelain for limited and for repeated voltage applications is another object of the paper. Tests were made with full and chopped waves and with steep impulses to simulate traveling waves and direct strokes. Line apparatus in particular frequently is subjected to these two types of impulses.

Test Method

For the impulse tests the equipment and general procedure conform to the usual practice. Voltage and time were recorded by the cathode-ray oscillograph connected across the test object through a resistance divider or a capacitance divider calibrated against the resistance divider. A rod gap chopped the impulse to the desired wave for the steep-front and chopped-wave tests applied to the specimens.

The voltage supply for the 60-cycle tests was a 150-kv 75-kva testing transformer excited through an induction regulator for voltage control. The voltage measured at the voltmeter coil of the testing transformer was calibrated against a standard sphere gap connected across the test load.

Porcelain Shells—Test Results

Forty-seven porcelain shells of the suspension-insulator type were tested in transformer oil at 24 degrees centigrade. The shells were nine inches in diameter and one-half-inch thick between the line electrode and ground plate, figure 1.

Tests were made to determine:

(*a*). The impulse strength for limited applications

(*b*). The impulse strength for repeated applications

(*c*). Sixty-cycle strength

Impulse strength for limited applications means single applications of voltage each setting raised in five-per-cent steps to breakdown. Table I and figure 2 give the results of these tests for positive and negative full, chopped, and steep-front waves for a total of 28 shells.

Impulse strength for repeated applications means as many as 100 applications of voltage each setting raised in approximately ten-per-cent steps to breakdown. Table II and figure 3 give the results for positive, full, and steep-front waves, and for negative full, chopped, and steep-front waves for a total of 13 shells.

For the 60-cycle tests a voltage of about 80 per cent of the expected failure was applied for one minute. A rest period of one minute intervened. The voltage was raised in five-per-cent increments to breakdown. This procedure was repeated for five shells. On one shell the voltage was rapidly applied to breakdown. The results are summarized in table III.

The breakdown of a single test specimen varies as much as ten-per-cent and more from the average, as shown in tables I, II, and III. For limited applications the variation on the average is five to ten per cent. The test data for repeated applica-

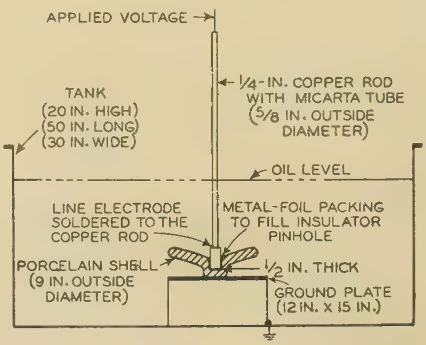

Figure 1. Arrangements of porcelain shells for test

tions are more consistent, the average variation not exceeding five per cent. Some of the 60-cycle breakdown values depart substantially from the average.

Porcelain has a characteristic similar

Table I. Impulse Strength of Porcelain Shells for Limited Applications

Porcelain Number	Test*	Number of Impulses	Kilovolts Held	Kilovolts Breakdown	Time to Breakdown (Microseconds)	Kilovolts Per Microsecond	Kilovolts Average Breakdown	Average Time to Breakdown (Microseconds)
1	Positive full wave $1^1/_2$x48 microseconds	6	160	168	3.0		154	2.9
3		1		137	4.0			
12		6	153	158	3.3			
5	Positive wave chopped at 2–3 microseconds	4	154	163	2.2		156	2.64
6		4	159	158	2.3			
7		3	148	159	2.5			
8		1		140	3.3			
9		6	171	179	2.5			
10		1		136	2.7			
11		4	141	153	3.0			
13	Positive wave-steep fronts	2	198	213	1.4	152		0.74
14		3	256	265	0.62	427		
15		1		279	0.59	473		
16		1		277	0.57	487		
17		1		279	0.29	967		
18		1		339	0.3	957		
24	Negative full wave $1^1/_2$x48 microseconds	11	199	205	2.3		219	2.9
25		5	185	194	2.3			
26		11	232	241	3.0			
27		10	225	235	3.0			
28	Negative wave chopped at 2 microseconds	7	201	209	2.0		209	2.3
29		8	210	209	2.5			
30		8	205					
19	Negative wave—steep fronts	1		339	0.2	1,690		0.37
20		1		330	0.19	1,735		
21		1		282	0.5	563		
22		1		298	0.53	563		
23		1		307				

* In these tests the full wave was $1^1/_2$x48 microseconds which for these tests is equivalent in effect to either the $1^1/_2$x40-microsecond (AIEE) or the 1x50-microsecond (IEC) waves.

Paper number 39-99, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE North Eastern District meeting, Springfield, Mass., May 3–5, 1939. Manuscript submitted August 8, 1938; made available for preprinting March 28, 1939.

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The authors acknowledge the helpful assistance rendered by G. Y. Hager, G. L. Candler, and W. D. Albright in the test work.

1. For all numbered references, see list at end of paper.

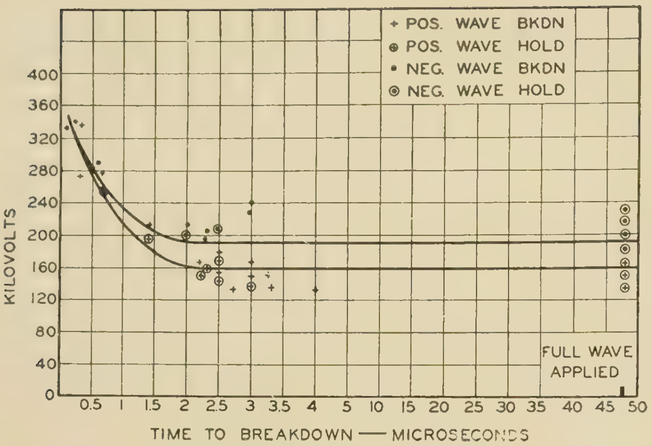


Figure 2. Impulse strength of porcelain for limited applications

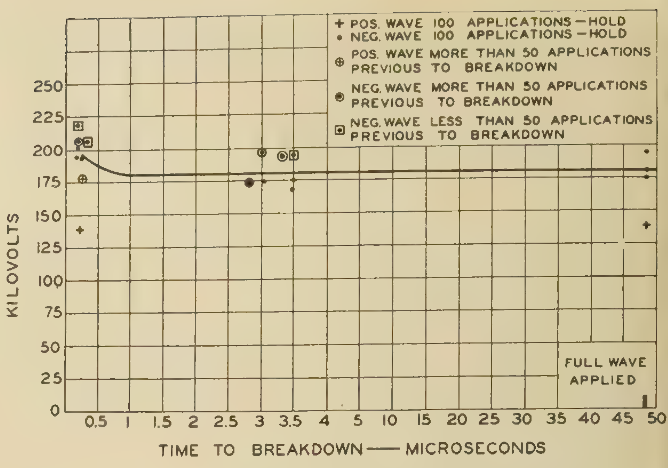


Figure 3. Impulse strength of porcelain for repeated applications

to other solid insulation materials in that for full waves or waves chopped on the tail breakdown occurs near the crest of the wave, from two to four microseconds (tables I and II).

For the majority of impulse tests the puncture of the porcelain (figure 5) was from the edge of the line electrode where the greater stress appears. In relatively fewer cases failure occurred near the center. The failure is in the nature of a fused path the size of an ordinary pin or greater through which the current discharged. When testing on the front of the wave, due to the higher setting of the impulse generator, holes were blown in the porcelain by the explosive action of the heavier current. Breakdown on 60 cycles was at the corner of the electrode.

From figures 2 and 3, a polarity effect is apparent for the specimen and electrode arrangement employed. For impulses, two microseconds and longer the negative voltage is about 20 per cent greater than the positive. At the very short time, for single applications, the polarity effect practically disappears.

The voltage-time characteristic for limited applications (figure 2) is a constant voltage for waves two microseconds and longer. For shorter durations, an upturn of the curve takes place with an increase in the voltage of nearly 100 per cent at 0.3 to 0.2 microsecond. These very short impulses are chopped on a front which rises at approximately 1,000 kv per microsecond (tables I and II).

For repeated applications (figure 3) the voltage-time characteristic is a constant voltage with the indication of an upturn on approaching durations less than one microsecond. The voltage strength for repeated impulses is about 90 per cent of that for limited applications for two microseconds and greater. These tests show that at 0.2 microsecond the single application breakdown is 70 per cent greater than for repeated applications.

The impulse ratio (impulse voltage divided by 60-cycle one-minute hold) of the flat part of the voltage-time characteristic for limited applications (figure 2) is respectively 1.47 and 1.75 for positive and negative waves. The average for the two polarities is 1.61. For repeated applications (figure 3) the impulse ratio is 1.28 for the positive and 1.65 for the negative waves. The average is 1.47.

Porcelain Tubes—Test Results

Tests were made on one-inch-thick porcelain. The specimens consisted of porcelain tubes, one-inch inside diameter through which a metal tube fitted snugly. Midway on the outside of the tube, a tinfoil band and flange similar to a bushing arrangement comprised the ground. Voltage was applied to the metal tube. The tests were made in transformer oil at 26 degrees centigrade. Tests at higher temperature did not show appreciable change in breakdown. The strength of several samples to a 1$^1/_2$x40-microsecond positive wave for limited applications averaged 237 kv and the one-minute 60-cycle hold, 146 kv (crest). Thus the impulse ratio is 1.62. This value compares closely with the corresponding

Table II. Impulse Strength of Porcelain Shells for Repeated Applications

Porcelain Number	Test*	Kilovolts	Number of Impulses	Kilovolts	Number of Impulses	Kilovolts	Number of Impulses	Kilovolts	Number of Impulses	Total Impulses	Kilovolts per Microsecond	Kilovolts Breakdown	Time to Breakdown (Microseconds)
37	Positive full wave 1$^1/_2$x48 microseconds	118	50	126	50	140	100	154	52	253		154	
41	Positive wave—steep fronts			132	100	139	100	176	54	255	765	176	0.23
31	Negative full wave 1$^1/_2$x48 microseconds			178	100	196	54			155		196	
32				175	50					51		175	
35				175	100	195	12			113		195	
33	Negative wave chopped at 2–3 microseconds			175	100	195	2			103		195	3.5
34				175	100	195	68			169		195	
36				175	51					52		175	2.8
44				171	100	195	88			189		195	3.5
38	Negative wave—steep fronts	150	100	165	100	193	99			300	918	193	0.23
39		150	100	165	100	193	200	218	22	423	800	218	0.18
42				176	100	202	90			191	1,040	202	0.2
43				176	100	200	109			210	1,000	200	0.2

* In these tests the full wave was 1$^1/_2$x48 microseconds which for these tests is equivalent in effect to either the 1$^1/_2$x40-microsecond (AIEE) or the 1x50-micro second (IEC) waves.

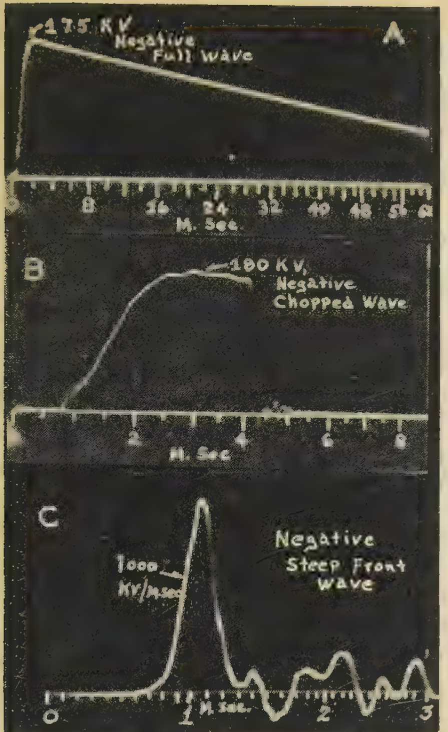


Figure 4. Typical oscillograms of test applied

A—Full wave
B—Wave chopped on tail
C—Steep-front wave

average impulse ratio for the tests on porcelain shells. From these and similar tests the relation between voltage strength and porcelain thickness is a straight line on log-log co-ordinates with a slope of 0.5.

Steep-Front Tests on Suspension-Insulator Strings

Strings of 3, 5, and 14 standard suspension insulators were flashed over with negative waves on a 3,000 to 4,000 kv per microsecond front. Thirty impulses in succession were applied to each string. The 14-unit string was last to be tested. Four insulators in this string already had been subjected to 30 impulses, so that these four units received a total of 60 impulses. The test data are summarized in table IV.

The five-insulator assembly was in addition subjected to combined steep-front and high-current flashovers (figure 6). This test simulates a lightning stroke discharge.[4] Table V summarizes the test data.

No sign of damage to the insulators was apparent from either the steep front or combined tests. These results are particularly significant as these tests approach or simulate in severity the stresses to which insulators may be subjected on lines as the result of direct strokes. On the basis of uniform distribution of the voltage, the insulators of the three-unit assembly were stressed the highest (table IV). Each insulator withstood repeated impulses of 330 kv with approximately 0.25-microsecond duration. The porcelain thickness of the insulators, between cap and pin, is approximately $^{13}/_{16}$ of an inch; the cap and pin are assembled to the shell with cement.

Table III. Sixty-Cycle Strength of Porcelain Shells

Porcelain Number	Test	Maximum One-Minute Hold Voltage: Kilovolts (Crest)	Maximum One-Minute Hold Voltage: Average	Breakdown Voltage: Kilovolts (Crest)	Breakdown Voltage: Average	Time Hold (Seconds)
45	60-cycle voltage applied one minute—one minute rest between steps	93.5		99		10
46		110.0		116		40
47		140.0	111	147	116	30
48		99.0		104		50
49		111.0		116		40
50	60-cycle voltage rapidly applied (approximately 10–15 seconds)			159		

Suspension Insulators Tested to Breakdown in Oil

Impulse tests to breakdown were applied to the insulators previously tested on steep fronts. Each insulator was immersed in transformer oil similar to the arrangement of figure 1, voltage being applied to the pin. The voltage was raised by steps to breakdown. Table VI summarizes the data.

Tests were made on 60 cycles. The results are given in table VII. In these as in the other tests reported in the paper good transformer oil (30 to 35 kv, standard cup test) was used, except for the three 60-cycle tests (table VII) where due to contamination the strength of the oil was 17 kv. Even then no apparent difference is noted in the puncture strength of the insulators.

The impulse tests indicate that the negative full-wave strength is somewhat higher than the positive full-wave but the data are insufficient to establish definitely the amount. The average breakdown for the positive and negative full waves is 265 kv. The average 60-cycle one-minute

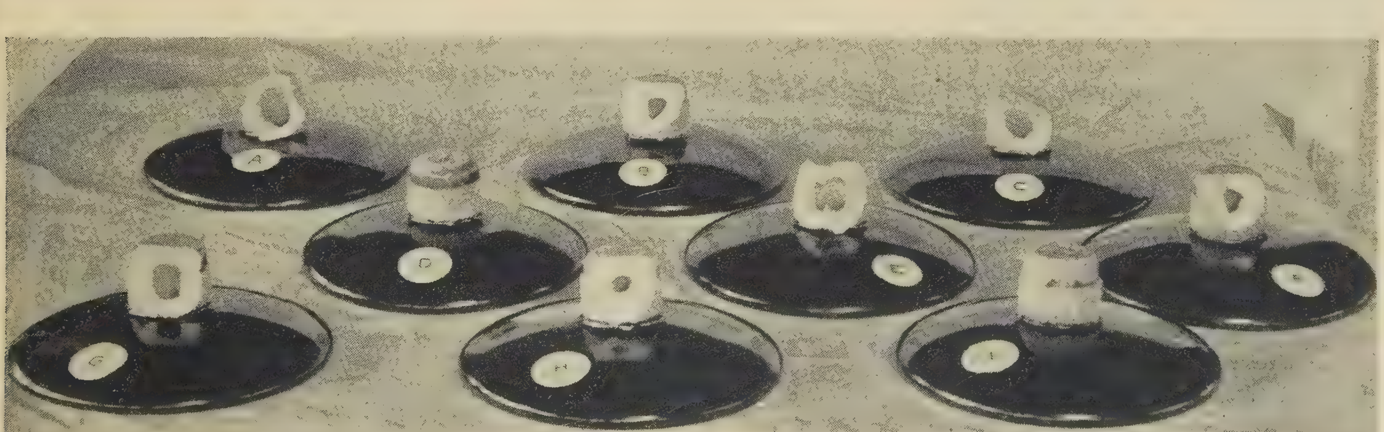


Figure 5. Typical breakdown of porcelain shells

Repeated applications:
A—Negative full wave
B—Negative wave chopped on tail
C—Negative steep-front wave
D—Positive full wave
E—Positive steep front

Limited applications:
F—Positive steep front
G—Negative steep front
H—Negative wave chopped on tail
I—Positive wave chopped on tail

Table IV. Steep-Front Impulse Tests on Suspension Insulator Strings, Negative Polarity

Insulator Units* in String	Number of Impulses	Voltage Applied: Kilovolts	Voltage Applied: Kilovolts per Microsecond	Comment
3	30	1,000	4,000	No apparent damage
5	30	1,300	3,000 to 4,000	No apparent damage
14	30	2,100	3,500**	No apparent damage

* Standard insulators, 10-inch diameter, $5^3/_4$-inch spacing.

** Rise of front. Flashover in 1.5 to 1.6 microseconds.

Table V. Combined High-Voltage and Current Impulse Tests on Five-Unit Insulator String*

Number of Tests	Voltage Applied: Kilovolts	Voltage Applied: Kilovolts per Microsecond	Current: Amperes	Current: Total Duration (Microseconds)	Comment
4	1,300	3,000 to 4,000	40,000	70	No apparent damage

* These tests made on same five-insulator string in table IV Polarity of voltage and current negative.

hold strength is 163 kv (crest). Therefore the impulse ratio is 1.62, a figure close in agreement with the results for the porcelain shells and tubes.

From these tests the voltage-time characteristic (table VI) rises at 0.2 microsecond approximately 50 per cent above the 2- to 40-microsecond value. This amount in the upturn is not so great as the tests of the porcelain shells (figure 2) show due possibly to the difference in the electrode arrangements and in the specimens, and in particular to the relatively larger number of impulses that were applied to the suspension insulators previous to (table IV) and during the breakdown tests (table VI).

Figure 6. Combined high-voltage and current impulse test on five-unit insulator string

Comparison of Porcelain Data

As a basis of comparison the data for the one-half-inch shells have been plotted in figure 7 as impulse-ratio/time curves. The corresponding impulse ratios for the one-inch tubes and the suspension insulators from 2 to 40 microseconds are essentially the same as in curve *A* (limited applications).

The 30 flashovers in air of the suspension insulator assemblies indicate that a stress was sustained by the insulator units which corresponds to an impulse ratio of 2.0 at 0.25 microsecond. This value compares to 1.7 of curve *B* where, however, the number of tests applied to the one-half-inch shells in oil is greater. Following the flashover tests, the limited tests on the suspension insulators in oil (table VI) give an impulse ratio of 2.0 at 0.28 microsecond and of 2.4 at 0.17 microsecond. Although the test data on the suspension insulators are not on the same basis as the curves of figure 7 and therefore cannot be directly compared, the data well substantiate the impulse-ratio characteristic of porcelain as given by these curves.

In a recent investigation for the comparison of impulse tests sponsored by the International Electrotechnical Commission, tests[5] are reported by Allibone on thin (0.15-inch) porcelain cups with an electrode arrangement simulating a rather uniform field. The tests were made in oil at room temperature. Repeated impulses were applied. Essentially the same voltage was obtained for positive and negative waves. From this investigation the impulse ratio for 1-, 5-, and 50-microsecond waves are respectively 1.55, 1.48, and 1.46. These values are in close agreement with the corresponding data in curve *B* of figure 7.

Sixty-cycle tests in oil of suspension insulators have been a subject of considerable investigation, for the condition of the oil affects the puncture voltage. Insulators tested in oil of abnormally low resistivity show some 30-per-cent increase in puncture voltage over the tests made in normal transformer oil, due apparently to the grading effect that low-resistivity oil has on the concentrated field at the metal parts. Rebora[6] has investigated the effect of the oil on the puncture voltage of standard suspension insulators. His data for the tests in normal transformer oil are in substantially good agreement with ours (table VII) both in regard to the relative values and in the nature of the breakdown. In these tests (table VII) where normal transformer oil was used no substantial grading at the cap edge or at the pin could have been present

Table VI. Impulse Strength of Suspension Insulators Tested in Transformer Oil

Insulator Unit	Test	Number of Impulses	Kilovolts Breakdown	Microseconds
A	Positive full wave	15	260	$1^1/_2$x40
B	Positive full wave	7	230	
C	Positive full wave	17	265	
D	Negative full wave	20	290	$1^1/_2$x40
E	Negative wave—steep fronts	16	320	0.28
F	Negative wave—steep fronts	3	395	0.17

Comments: Oil temperature 24 degrees centigrade. Dielectric strength of oil 30 kv in standard cup. 0.1-inch gap, one-inch disks. Time to breakdown of *A*, *B*, *C*, and *D*, three to eight microseconds. Failure of *A*, *B*, *D*, and *E* from cap edge to pin. Failure of *C* and *F* inside cap to pin.

since streamer formation could be observed as the test voltage was increased to the puncture value. The concentrated stress is indicated also from the frequent occurrence of failure through the porcelain shell from the edge of the cap to the pin. The flashover data of the insulators (in air) point out the possibility that for air the corona and streamers from the cap and pin are of such a nature as to have a grading effect. By virtue of this grading effect a higher stress would be sustained by the insulator (tables IV and VI).

While the dielectric strength of porcelain is affected by and varies with the test specimen, the electrode arrangement, the method of test, and other factors, the data presented in this paper and elsewhere establish that the curves of figure 7 are representative of the voltage-time characteristic of porcelain.

Apparatus in Service

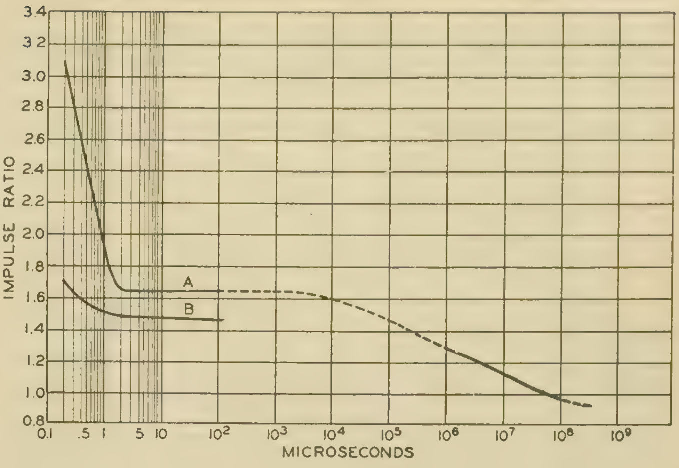
The voltage-time characteristic of porcelain is apparent also from the performance of apparatus in service. As an illustration, suspension line insulators will be considered since this apparatus is frequently stressed in service from traveling waves and direct strokes. Moreover, line insulators are well standardized and their flashover characteristics fully established.[7]

The porcelain thickness between cap and pin for standard insulators is about 0.80 inch. For the 1.5x40 microsecond wave flashover the average stress per insulator is from 100 to 90 kv for insulator strings of 5 to 16 elements. The average stress per insulator for a two-microsecond impulse is approximately 130 kv. These stresses are for equal voltage distribution over the insulator string and require multiplying by a factor greater than unity for departure, from uniform distribution. It is conceivable also that in the process of flashover stresses exceeding these average values would appear across the individual insulators for the very short time in which the flashover occurs. All considered, the good performance of modern line insulators to traveling waves is naturally expected.

Experience shows that even direct strokes seldom, if ever, puncture the porcelain in modern suspension insulators. Consider the probable stress of the porcelain from the various records of direct strokes to the line. An oscillogram of a direct stroke to a 220-kv line[8] (16-insulator assembly) indicates a rate of rise of 1,500 to 2,000 kv per microsecond, the voltage reaching a crest of 3,000 kv. Other records and analyses[9,10,11] indicate that rates of rise up to 5,000 kv per microsecond possibly are attained.

Considering 3,000 kv per microsecond as or near the upper limit for the steepness of the front of lightning strokes, from table IV and other published data[4,12,13], the average stress and its duration per element for a 16-insulator string would be approximately 200 kv and 1.2 microsecond. For a 5-insulator string the average stress and duration are 280 kv and 0.5 microsecond and for a 3-insulator string, 330 kv and 0.25 microsecond. The ability of insulators to withstand these stresses is demonstrated from the tests reported in this paper (tables IV and V) and in a previous investigation.[4]

Figure 7. Impulse ratio-time characteristics of porcelain for limited applications (A) and for repeated applications (B)



The good performance of line insulators even when subject to direct strokes is quite understandable from fundamental considerations of the dielectric strength of porcelain. Furthermore, this analysis indicates that the steepness of the front of direct strokes possibly is not so great as assumed in the past.

Conclusions

1. The voltage-time characteristic of porcelain for limited applications is a constant voltage down to two microseconds and rises with shorter impulses nearly doubling at 0.2 microsecond.

2. For many repeated applications, the voltage-time characteristic is practically constant with relatively smaller upturn at the shorter impulses.

3. The impulse ratio on the flat part of the characteristic is 1.60 for limited applications and approximately 1.45 for repeated applications.

4. These inherent characteristics of porcelain are apparent also from the good performance of line and other apparatus subjected in service to traveling waves and direct strokes.

5. The characteristics of porcelain compared with its good performance in service indicate that the steepness of strokes on lines possibly does not or seldom exceeds 3,000 to 5,000 kv per microsecond.

Table VII. Sixty-Cycle Strength of Suspension Insulators Tested in Transformer Oil

Insulator Unit**	Test	Maximum One-Minute Hold Voltage (Kilovolts Crest)	Breakdown Voltage Kilovolts Crest	Breakdown Voltage Time Hold (Seconds)	Remarks*
G	60-cycle voltage applied one minute. One minute rest period between steps	132	145	55	Oil temperature 25 degrees centigrade. Oil strength 35 kv
H		161	166	35	
I		168	175	45	Oil temperature 20 degrees centigrade. Oil strength 17 kv
J		168	175	30	
K		161	168	10	
L		175	182	50	Oil temperature 20 degrees centigrade. Oil strength 31 kv
M		168	175	15	
N		161	168	45	
O		168	175	55	
		Average = 163			

* Dielectric strength of oil determined in standard cup. 0.1-inch gap between one-inch disks.

** Failure of *G, H, I, J, L, N,* and *O* from cap edge to pin; of *K* and *M* from inside cap to pin.

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12. Factors Influencing the Insulation Co-

Line Problems in the Development of the 12-Channel Open-Wire Carrier System

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Synopsis: The development of the type *J* 12-channel carrier telephone system for open-wire lines required an increase of nearly 5 to 1 in the transmission frequency range of the lines. In the provision of suitable line facilities a number of new problems were encountered with respect to attenuation, noise and cross talk. Methods for meeting these problems and the results obtained are described.

A NEW carrier telephone system for open-wire telephone lines has been described recently.[1] This system increases the number of two-way telephone circuits which can be obtained on a single pair of wires from the previous maximum of 4 to a total of 16. This has been achieved by extending the frequency range from a maximum of about 30 kilocycles to more than 140 kilocycles. The exploitation of this new range of frequencies on open wire has involved the solution of a number of interesting problems, among which are these:

1. Not only does the attenuation of an open-wire line under ordinary weather conditions rise substantially with frequency but extremely large increases in attenuation occur at the higher frequencies when ice forms on the wires.[2,3] In spite of these effects a high degree of stability of transmission has been secured on all channels by the provision of automatic control of repeater gain and equalization.

2. New cross-talk problems created by the extension of the frequency range have been solved by the development of transposition designs with numbers of transpositions not greatly in excess of those employed for the lower-frequency systems. Problems have also arisen in controlling the cross talk around the repeaters and in reducing the effect of impedance departures between the line circuits and the equipment.

Paper number 39-80, recommended by the AIEE committee on communication, and presented at the AIEE South West District meeting, Houston, Tex., April 17–19, 1939. Manuscript submitted February 15, 1939; made available for preprinting March 13, 1939.

L. M. Ilgenfritz, R. N. Hunter and A. L. Whitman are all members of the technical staff of Bell Telephone Laboratories, Inc., New York, N. Y.

1. For all numbered references, see list at end of paper.

Frequency Allocations

The type *J* system operates on circuits on which type *C* carrier systems were already operating in the frequency range up to about 30 kilocycles. To provide enough frequency separation between the two systems the lower frequency limit of the *J* system was set at 36 kilocycles; the necessary frequency space for 12 channels in each direction set the upper limit at about 140 kilocycles. This range is split into two parts, one used for transmission in one direction and the other for the opposite direction. Figure 1 illustrates the relation of the frequency bands occupied by the type *J* and type *C* systems and the voice-frequency channel. Different "staggered" locations of the frequency bands are to be employed in order to simplify cross-talk problems.

Filters are used for separation of the type *J* from the type *C* and lower-frequency facilities on the same pair of wires. This separation is done by means of a combination of high- and low-pass filters which split apart the frequency ranges above and below the band between 30 and 36 kilocycles. To simplify the design of these filters, the low-frequency group of the type *J* system is transmitted in the same direction as the high-frequency group of the type *C* system. This arrangement of transmitting certain frequencies in a particular direction is generally used throughout the telephone plant in order to avoid serious cross-talk difficulties. Accordingly with few exceptions west to east transmission or south to north transmission takes place in the same frequency bands throughout the country and similarly, east to west or north to south transmission employs the same frequency bands. These are indicated in figure 1.

Line Attenuation

An open-wire pair affords the lowest-loss transmission medium of any conductor employed in the telephone plant. It is, however, peculiarly subject to the effect of weather, which may cause large and often rapid changes in the attenuation. In consequence, some form of gain regulation is required.

Even for carrier systems operating up to 30 kilocycles, manual regulation is inadequate for the longer systems and automatic devices have been provided for

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Discussion

Victor Siegfried (Worcester Polytechnic Institute, Worcester, Mass.): This paper is of interest in that it fills in a gap in the information on dielectric strengths of materials where no great amount of data exists. The behavior of porcelain is shown to be similar to that of other solid dielectrics in the volt-time characteristics, and in the difference in characteristics on repeated application as compared with a limited number of voltage applications.

In obtaining their data, the authors have used porcelain shells without hardware. I am wondering what difference might be expected when a cap is on the units, giving a more even distribution of stress on the inside corner next to the pin. It might be expected at first guess that the characteristics would be similar to those found, but at different values for the actual breakdown voltage, thus giving a higher actual voltage which the unit could withstand. If there is a tangible difference, it will affect the extension of these data to other cases where the actual porcelain insulator with line hardware is used.

In general, this paper shows the ability of the modern insulator to stand up under the severest stresses imposed by line conditions; in fact, the authors very significantly conclude that the proved ability of the insulators to take strokes in service indicates a maximum steepness of wave of 3,000 to 5,000 kv per microsecond. This shows us that the laboratories cannot go wrong in speeding up the wave fronts to such a value when attempting to duplicate the worst type of impulse that natural lightning is likely to produce.

P. L. Bellaschi and **M. L. Manning**: The tests on complete porcelain units to which Mr. Siegfried refers are given in tables IV to VII inclusive. One of the objects of the paper was to establish the volt-time characteristic of porcelain. Accordingly, plain shells, completely assembled in insulator units, tubes, etc., were tested. In these tests, different electrode arrangements were used for the various specimens. The average results of such tests are given in figure 7, which is the representative volt-time curve of porcelain.

We are in full agreement with Mr. Siegfried in that the tests verify the good performance of porcelain expected and found in service.

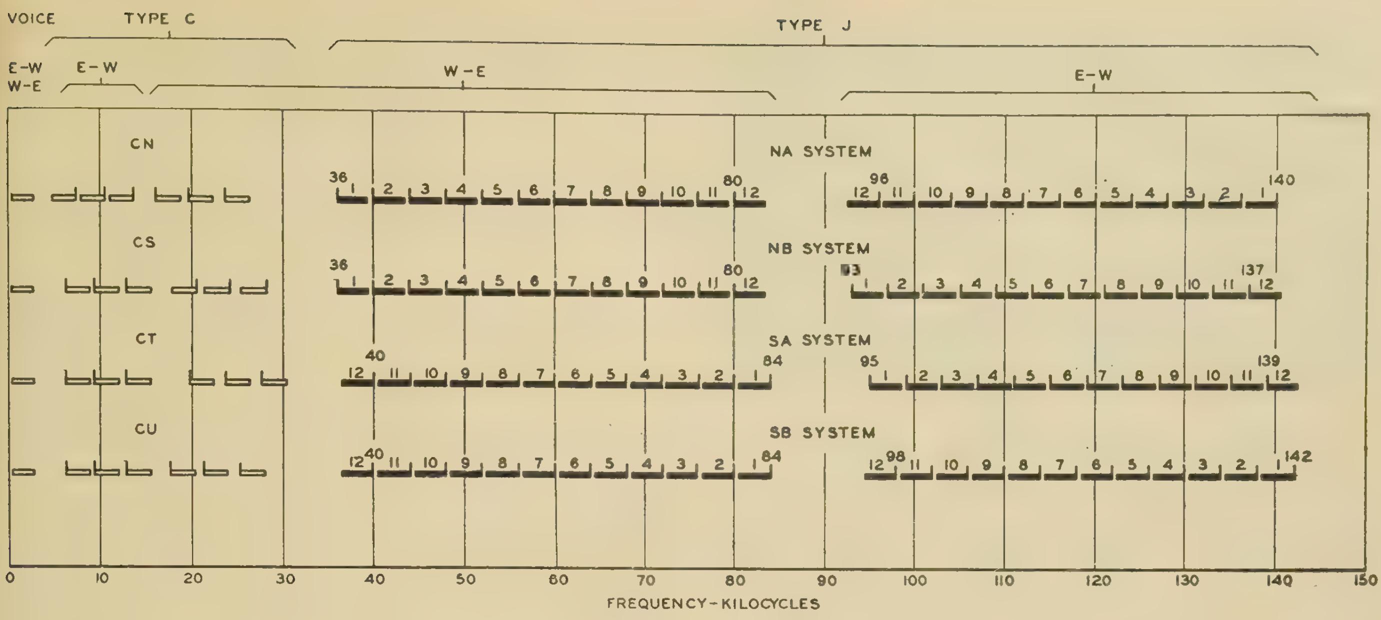


Figure 1. Frequency allocation

Note: E-W also implies transmission N-S and W-E implies S-N

most systems over 500 miles in length. The attenuation changes caused by changes in resistance of the wire with temperature or by changes in the shunt losses when insulators become wet are much larger at the higher frequencies of the *J* system, and therefore, an automatic regulating scheme is required. Tests were made on open-wire circuits to determine more precisely the characteristics needed for such a regulator. During sleet storms, when wires are covered with ice, the increases in attenuation are far beyond any caused by rain. Figure 2 shows increases which may be

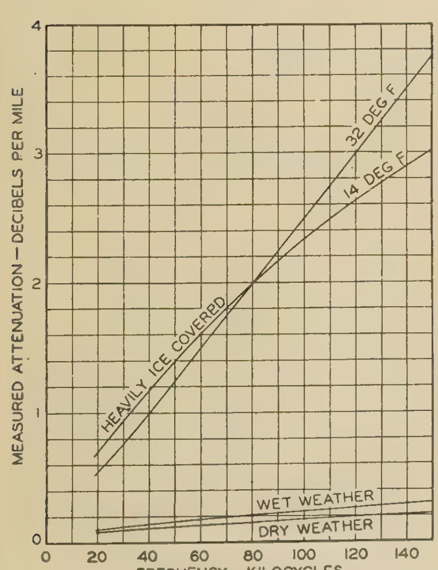


Figure 2. Attenuation variation with weather

Eight-inch spacing, 165-mil copper wire

caused by ice as compared with the normal dry- and wet-weather values.

The deposits on the wire may be actual ice, or in some cases wet snow or frost adhering to the wire. Figure 3 shows an example of such deposits. Theory shows that the increase in attenuation is caused by energy losses in the ice itself and that leakage across the insulators is usually a negligible factor.

An extensive survey of the effects of ice has been carried on at various points throughout the country during the past four years and a large amount of information has been accumulated. These tests have shown that the shape of the attenuation-frequency characteristic differs considerably for different ice formations and even if the ice deposit remains the same for a time, the attenuation-frequency characteristic may vary with temperature as in figure 2. The two upper curves of the figure were measured at different times during the same storm. There was no apparent change in deposit between the two measurements. This change in shape of the characteristic, of course, makes the regulation problem more difficult. In spite of the extreme severity of ice effects in certain regions it is expected that satisfactory reliability will be obtained on type *J* systems by placing the repeaters sufficiently close together.

Regulation Problem

In the first type *J* systems the regulator actuated by a single pilot frequency in each direction compensates for the attenuation changes caused by temperature and wet weather.

The required varieties of attenuation slopes with ice on the wires could not be provided by a simple regulator. Hence provision is to be made in later designs for a regulator with variable slope controlled by two pilot frequencies which is expected to be satisfactory in areas subjected to sleet conditions. The regulating range will also be increased so that a completely automatic control of gain up to about 75 decibels will be available.

It was found that during periods when ice coated the wires the circuit noise measured at the end of a repeater section usually decreased as the attenuation increased. This is important because otherwise the extra increase in the repeater gain to take care of the higher attenuation at such times would make the noise excessive. The study of ice conditions throughout the country which has been carried on and is still continuing will be useful in laying out repeater stations along some of the routes which eventually will be candidates for the application of type *J* systems.

Open-Wire Cross Talk

The cross-talk problem on open-wire lines is one of the most important. Cross talk is controlled by transpositions which are introduced into the various pairs in accordance with a predetermined design. The creation of the necessary designs requires consideration both of the complex theory of transpositions and measurements on lines constructed by practical methods.

However, the design of transposition systems is considerably simplified by the use of different frequencies for the two directions of transmission. The only cross talk between systems which is

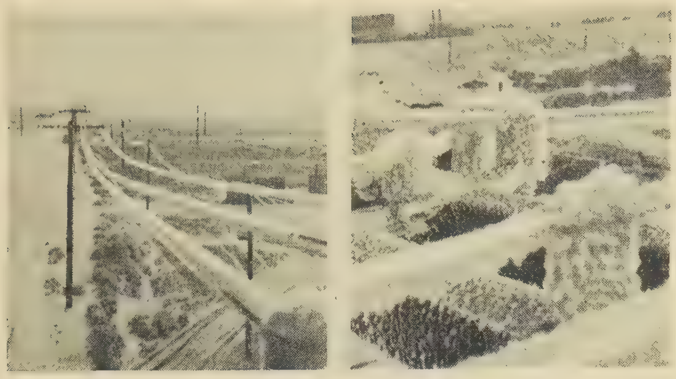


Figure 3. Ice on wires and insulators near Amarillo, Tex.

directly important is that known as far-end cross talk, which is that between a talker at one end of one circuit and a listener at the opposite or far end of another. Near-end cross talk, which is that between a talker and a listener at the same or near ends of two circuits, becomes a source of interference between circuits only when portions of it appear as far-end cross talk because of reflections at points of impedance irregularity in the circuits.

Because of the high cost of a transposition design to keep both near-end and far-end cross talk down to small values, only small reflections are permitted where open-wire and cable meet, or where circuits are terminated in equipment. A number of the difficulties which had to be overcome to attain small reflections are discussed later in the paper. With this control the transposition designer can concentrate most of his attention on far-end cross talk, the near-end cross-talk requirements are relaxed, and a cheaper transposition arrangement can be used.

What can happen when reflection occurs may be seen by comparison of the near-end and far-end cross-talk curves in figure 4. The similarity in the shapes of the two curves, and particularly the fact that the peaks occur at the same frequencies, show that what appears to be far-end cross talk is in this case mostly reflected near-end cross talk. It is for pair combinations such as this one, where the near-end cross talk is much larger than the far-end, that the closest control of reflection effects is required. With the values of reflection realized in the *J* system, reflected cross talk will ordinarily be unimportant.

To obtain satisfactory cross-talk conditions at the higher frequencies some changes in line construction are necessary. To use type *J* carrier systems on existing open-wire routes, methods were devised for modifying the line construction in as economical a manner as possible. For new lines, such as the new part of the fourth transcontinental line[5] advantage was taken of the greater degree of freedom in structural design which was possible.

Figure 5 shows three types of open-wire pole head configuration suitable for *J* system operation. The left-hand diagram shows a method of reconstructing part of one of the older types of open-wire lines built with 12-inch spacing between wires of the pairs and with the "alternate arm" transposition system which was developed for the use of type *C* systems on the side circuits of the horizontal phantom groups on alternate arms. This method is a flexible one in that one or more phantom groups may be converted at a time, as on the second crossarm shown. For such an application not only was removal of the phantoms and retransposition necessary, but the spacing of the two wires of each pair was reduced to six inches. This general method of construction was used for the Dallas-Houston and Dallas-San Antonio lines,[6] except that the six-inch pairs were constructed with new wire on a new crossarm rather than by respacing 12-inch pairs.

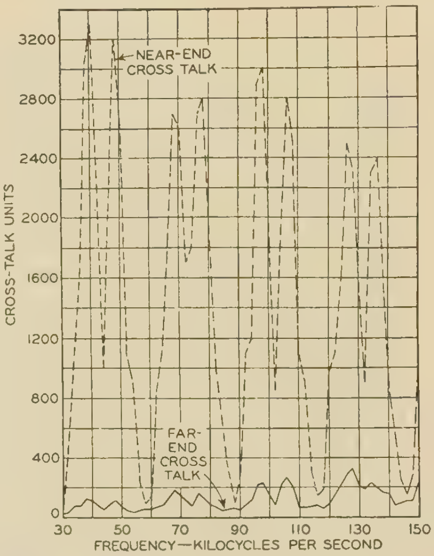


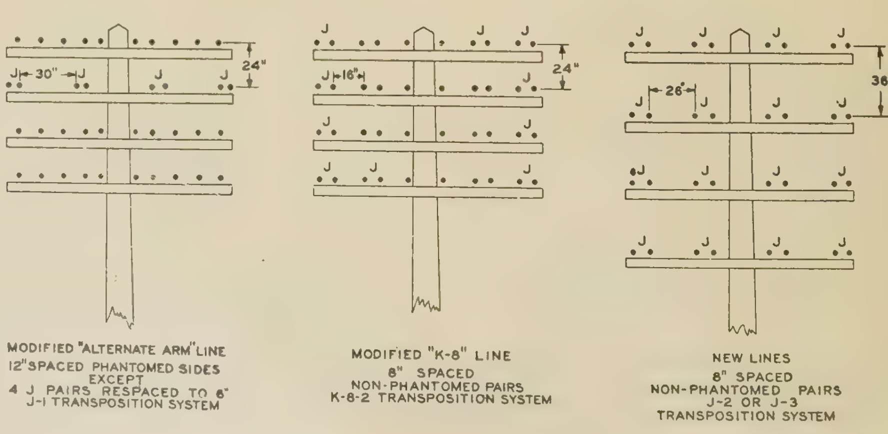
Figure 4. Near-end and far-end cross talk—J-3 transposition system

Pairs 3/4–9/10

Another common type of open-wire pole-head configuration, the middle diagram of figure 5, is that made up of eight-inch-spaced nonphantomed pairs transposed in accordance with the *K*-8 transposition system on an eight-span base. Through design studies supplemented with field experiments it was found that such a line could be converted for *J* systems much more cheaply than an alternate-arm line. If *J* systems are restricted to the pairs on the outer ends of the crossarms, with two inner pairs, about one or two transposition changes in each pair per mile are enough. This scheme was followed in reconstructing the line between Charlotte, N. C., and West Palm Beach, Fla.

For new lines yet to be built, a greater degree of latitude in structural design is naturally possible. The right-hand diagram of figure 5 shows an open-wire pole-head configuration designed to allow *J* systems to be operated on all of the pairs. The unique feature of this configuration is that, while 8-inch spacing is preserved between the wires of the various pairs,

Figure 5. Three types of open-wire pole head configuration



the adjacent nonpole pairs on a crossarm are separated by 26 inches and the crossarms by 36 inches. The reduction in coupling made possible by this increased spacing keeps the cross talk for any combination of pairs down to a suitable value with transposition arrangements not necessarily more complicated than those employed for the other configurations. This type of construction was used for the new parts of the fourth transcontinental line.

Figure 6 shows a comparison of the number of transpositions used in a typical section of open-wire line for various types of circuits from voice-frequency phantomed circuits to nonphantomed circuits intended for *J* system operation. From the original arrangement where there was one transposition point in every ten spans, about one-fourth mile, the number of transpositions for *J* carrier operation has been increased so that for the *J*-3 design, which was used for the new wires on the fourth transcontinental line, there are four transpositions in each eight-span interval and every pole is a potential transposition point.

It may be seen from figure 8, however, that the number of transpositions required in pairs for *J* carrier operation is not necessarily larger than the number employed in systems intended for *C* carrier operation with a top frequency of 30 kilocycles. The superiority of the *J* system transposition arrangements as compared with those designed for *C* system operation results from the choice of specific arrangements which best limit the systematic effects for frequencies in the *J* system range.

Typical far-end cross talk measured between eight-inch-spaced pairs 11–12 and 19–20 on a new *J*-3 line and on a reconstructed *K*-8-2 line is shown by figure 7. The superiority of the new line with its fewer wires, greater wire separations, better transposition system, and smaller irregularities is evident.

Absorption Effects

The attenuation of an open-wire pair may be quite unsatisfactory if there are what are known as absorption effects, caused by induction into surrounding circuits such that energy is absorbed in particular frequency bands and the attenuation of the pair increased. These effects, which depend on the transposition arrangements in the circuits, may cause objectionable transmission distortion at critical frequencies unless the transpositions are planned to avoid them. The same arrangements necessary to control cross talk between *J* systems will automatically eliminate absorption effects with one exception. If only part of the pairs on a line are designated and transposed for *J* systems and the remaining pairs are not so transposed, absorption in a *J* pair can be caused by a nearby non-*J* pair. Consequently, consideration of the cross-talk relations at *J* frequencies between all of the pairs on the line cannot be avoided even though some of them will not be used for *J* systems.

Figure 8 illustrates the effect of absorption on three different pairs. Curves *A* and *B* show the absorption measured over the type *J* frequency range on a line of the alternate-arm type. Curve *A* was obtained on a side circuit transposed for operation at frequencies only up to about 10 kilocycles. The absorption at frequencies above this becomes very large. Curve *B* shows the absorption present on one of the *C* carrier side circuits on the same line transposed for operation up to 30 kilocycles. Curve *C* shows how absorption disappears on a nonphantomed pair specially transposed for type *J* operation. If this pair were measured at much higher frequencies, similar absorption "bumps" would be found, perhaps at frequencies of 200–300 kilocycles or higher.

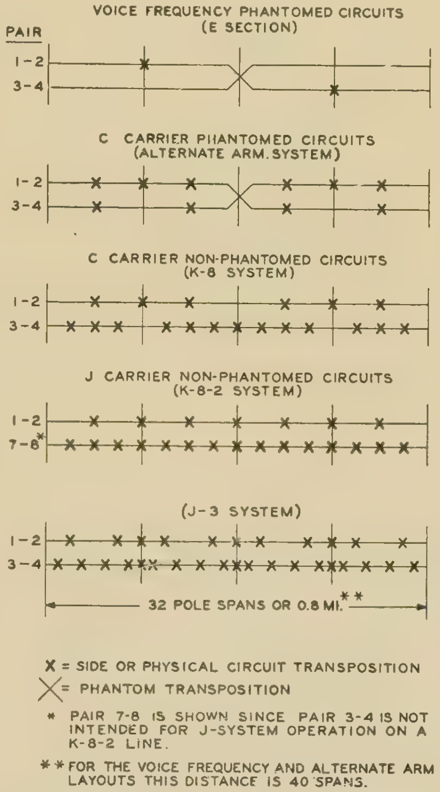


Figure 6. Illustrative transposition arrangements

Since absorption effects depend on the systematic addition of cross-talk currents along a line, a continuous succession of identical transposition sections tends toward greater absorption while a random succession of different kinds of transposition sections of different lengths will reduce it. The Dallas-Longview *J* system is operating on an alternate-arm side circuit, transposed for *C* carrier operation and without any modifications to adapt it for the higher frequencies. Because of the fortunately irregular succession of different transposition sections found here, it was possible to select, after tests, a pair with no serious absorption.

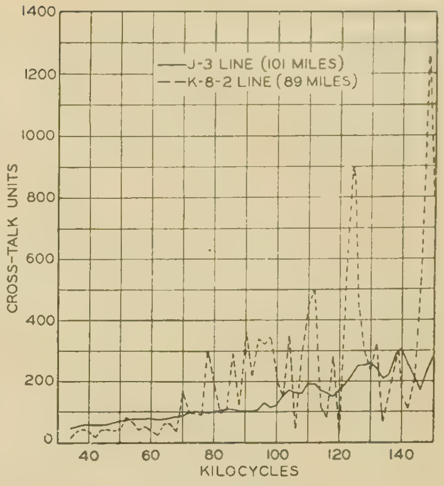


Figure 7. Far-end cross talk between eight-inch-spaced pairs 11/12 and 19/20

Construction Irregularities

With the new transposition designs, the systematic cross talk resulting from the transposition arrangements has been reduced in nearly every case so far that the remaining cross talk is controlled principally by construction irregularities. An important source of irregularity is the difference in sags of the various wires in each span of the line, particularly sag differences between the two wires of each pair. Another potentially important source of irregularity is the variation in the spacings between successive transposition poles. It is relatively easy to make this factor unimportant as compared with sag differences.

The large amount by which the cross talk can be reduced by careful methods of construction coupled with the highly developed systematic transposition patterns is illustrated by the fact that between certain pairs the cross talk in a 75-mile repeater section is reduced to a value which would be produced by a capacitance unbalance between them of less than two micromicrofarads, which is about the same in magnitude as the capacitance between wires of a foot of the

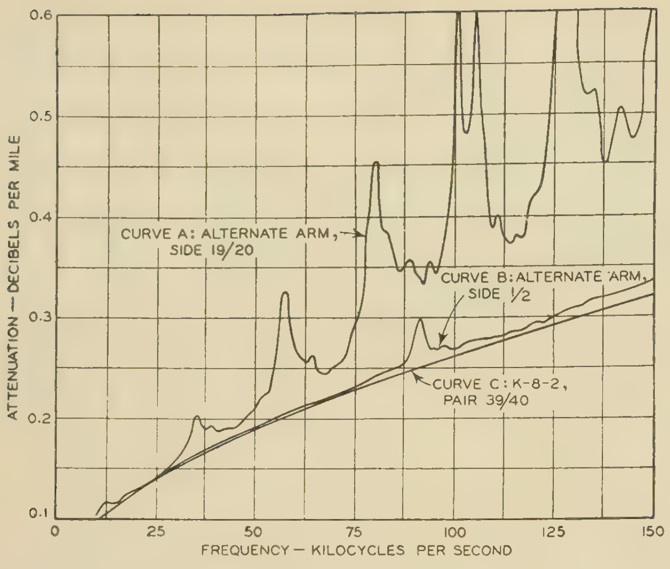


Figure 8. Attenuation of open-wire pairs of different types

Curves *A* and *B*—Side 19/20 and 1/2, respectively, on alternate-arm line, 104-mil, 12-inch, 56.7 miles, 90 degrees Fahrenheit, at Mascoutah, Ill.

Curve *A* is transposed for voice frequencies

Curve *B* is transposed for carrier operation up to 30 kilocycles

Curve *C* is pair 39/40 on K-8-2 line, 104-mil, 8-inch, 50 degrees Fahrenheit and CS insulation between Denmark, S. C., and Rincon, Ga., transposed for carrier operation up to 140 kilocycles

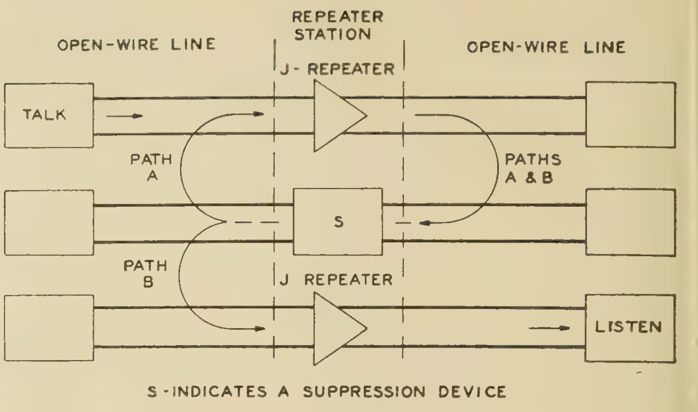


Figure 9. (above). Interaction cross talk at a J repeater station

open-wire pair. This large cross-talk reduction is in spite of the fact that at 140 kilocycles the phase change along an open-wire circuit is about 7 degrees in a single span, the shortest distance between any two transpositions, and about 28 degrees for the more common four-span interval.

Interaction Cross Talk at Repeater Points

Another type of problem was introduced by what is known as interaction cross talk. This is the cross talk which occurs from one side to the other of a *J* repeater station. Figure 9 illustrates two paths which it may take. Path *A* shows the cross talk from a system to itself which may cause transmission distortion or repeater singing while path *B* is the path of cross talk between different circuits. The essential feature of this interaction cross talk is that, as figure 9 shows, the cross-talk path at a repeater station passes through the *J* repeater and hence the cross talk is amplified by the repeater gain.

The new problems of controlling this cross talk were the result of larger magnitudes of cross talk at the higher frequencies, the larger repeater gains, and the fact that with more repeaters there were more points on a system where it could occur. Magnitudes of interaction cross talk which had previously been thought of as inconsequential assumed a new importance. For instance, with the gain of about 75 decibels proposed for the repeater for use in sleet areas, an initial value of unamplified interaction cross talk as low as 0.25 cross-talk unit would be magnified to 1,400 units, which might considerably exceed the far-end cross talk existing at the same time in one repeater section.

Several new methods for reducing this interaction cross talk were devised. In the first place, in order to prevent direct coupling between the wires of the open-wire line on the two sides of the station, it was found necessary to cut a gap in the line. With the wires entirely removed for a distance usually of about 80 feet, the line is brought into the station from the two terminal poles by means of the lead-in cables.

It was also seen to be necessary to block the paths provided by the wires of the telephone line itself. For this purpose, cross-talk suppression filters were designed and built to be installed in all of the non-*J* circuits on the line. These give losses of the order of 70 decibels at 140 kilocycles not only in the metallic transmission circuits but also in other circuits, made up of various combinations of the line wires, which may conduct cross-talk currents through the stations.

In addition to the cross-talk suppression filters and in order to provide an extra margin of safety against interaction cross-talk currents which might find their way through the repeater station by stray paths, longitudinal choke coils have been connected at the pole heads between the open wires and the lead-in cables. These coils do not disturb ordinary transmission but add high impedance in the longitudinal circuits.

These measures for controlling interaction cross talk have been found to be adequate so far as the telephone line is concerned. At an occasional *J* repeater station, however, located on a right-of-way occupied by several pole lines, there is found another pole line paralleling the telephone line with a separation sometimes as little as two to five feet between the nearest wires of the two lines. Such wires provide other interaction cross-talk paths past the repeater station and impair the effectiveness of suppression measures installed in the line on which the *J* system is operated. The by-passing effects of such a foreign line can be controlled by cross-talk suppression devices similar to those used in the telephone line wires.

Figure 10 shows a comparison of the interaction cross talk measured at a *J* repeater station before any suppression measures were installed, the other wires of the line being continuous at the station location, with the corresponding interaction cross talk when the line was run through the suppression devices in the station. The values shown would be amplified by the gain of the *J* repeater on the disturbed circuit before they reached the listener. The effect of the by-passing foreign line is illustrated by the difference between the middle and bottom curves, the bottom curve showing the measured cross talk when the by-passing line was cut to simulate the effect of suppression measures in it.

Staggered Systems

It would not be possible with the open-wire line configurations now in use to design transposition arrangements that

Table I

Transposition System	Wire Spacing (Inches)	Noise (Decibels)*	Repeater Spacing in Miles—128-Mil Wire
Alternate arm	12	+10	67
K-8-2	8	+ 5	82
J-1	6	− 2	103

* Above reference noise, 10^{-12} watt at 1,000 cycles.

would permit the operation of identical *J* systems on all pairs. For this reason four types of *J* systems with different channel carrier-frequency allocations will be provided in the future. The frequency assignments for these systems are shown in figure 1.

The "staggering" advantage, or effective cross-talk reduction between systems, is effected because (1) the inversion or displacement of channels in the different systems with respect to each other makes the cross talk unintelligible, and (2) the reduction of the overlap between channels results in less energy being transferred between them by cross talk. The net benefits of "staggering" obtained by the allocations shown in figure 1 range from about 6 to 16 decibels.

The most effective pair assignments for the four types of *J* systems can best be obtained from actual cross-talk data on the particular sections of line involved. The "staggering" advantages obtained are sufficient so that the highest remaining cross talk will usually occur between the like *J* systems operating on nonadjacent pairs.

Noise

Observed external sources of noise in *J* systems are atmospheric static, dust storms, radio stations, power-line carrier, and power-supply systems.

Of these possible sources the more important will usually be atmospheric static which will be greatest during the summer months. In regions where dust storms occur, their effects are expected to exceed that of atmospheric static but will be more likely to occur during the winter and early spring.

Table I shows values of noise at 140 kilocycles, caused by atmospheric static, found at the open-wire line terminals of one repeater section; the values are those which it is expected will be exceeded during one per cent of the summer season extending from May to September. If the repeater spacings shown were used, the total static noise in the top channel at the end of a circuit with 20 repeaters would be 20 decibels above reference noise at the −9-decibel level. However, other factors such as ice may require the use of shorter spacings.

Line Impedance

As mentioned previously in the discussion of cross talk, it is important that the line impedances be matched closely and large irregularities be avoided. Because of the different wire sizes and pair spacings, a wide range of open-wire line impedances may be encountered. Novel construction arrangements and the development of new lead-in circuits have made it possible to secure a reflection coefficient of about five per cent at the junction between the open-wire pair and the toll entrance and office equipment at the highest transmitted frequency.

The transposition arrangement and wire spacing of a pair affect the smoothness of its impedance because they affect the reactions between circuits which

Figure 10. Unamplified interaction cross talk between two J circuits at an auxiliary repeater station

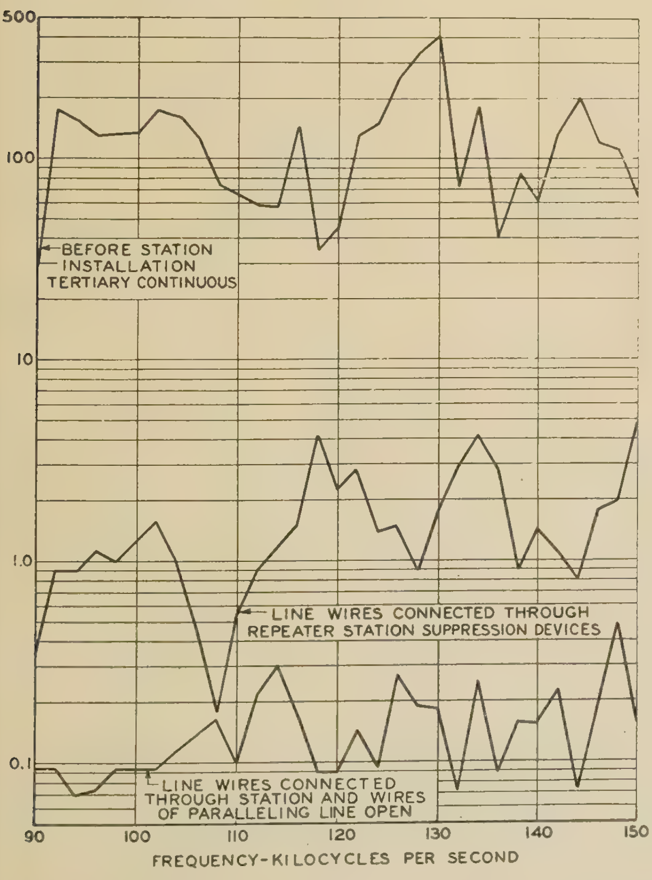
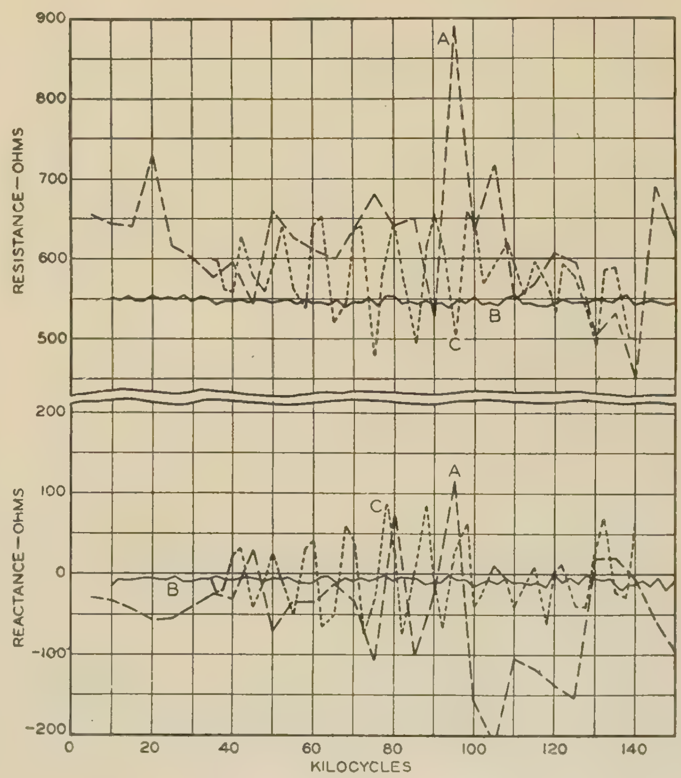


Figure 11. Impedances of open-wire pairs of different types

A—104-mil, 12-inch-spaced, side 17/18 of an alternate-arm line

B—165-mil, 8-inch-spaced, pair 17/18 of new *J*-3 line

C—128-mil, 8-inch-spaced, pair 31/32 of K-8-2 line with miscellaneous lengths of tree wire, etc.



cause absorption effects. The marked improvement which can be obtained by proper design is illustrated by comparison of curves A and B of figure 11. Curve A shows the impedance of a 12-inch-spaced side circuit on an alternate-arm line. This particular circuit was one intended for use at frequencies not above ten kilocycles. In striking contrast curve B shows the comparatively smooth impedance of an eight-inch-spaced nonphantomed pair on a new line transposed in accordance with the J-3 system.

"Tree" wire, a special line wire with abrasion-resistant insulation, has been used on open-wire lines for many years in places where the lines were exposed to tree branches. During line tests in Florida, another use for tree wire was found where the open-wire line, along a causeway or bridge, is subject to fouling by fishing tackle. Curve C of figure 12 shows what a half-mile or so of this tree wire, supplemented by several sections of 165-mil wire at railroad and power-line crossings, can do to the impedance of a 128-mil pair. To reduce the irregularities a new type of insulated line wire of smaller diameter and with thinner insulation was developed. This wire has about the same impedance characteristic as the line wire.

Intermediate Cable Treatment

When open-wire lines have to be placed underground to pass through towns or to cross natural barriers such as rivers which cannot be spanned economically with open wire, cable is used. In the past, the circuits in such cables were frequently loaded to reduce their attenuation and to match the impedance of the open-wire circuits in order to avoid reflection effects and degradation of voice-frequency repeater balance. To load paper-insulated cable pairs for frequencies up to 150 kilocycles would require exceedingly short loading spacing, of the order of 200 feet, which would be expensive and in many cases impractical with existing manhole locations. An alternative, the use of a transformer to match the open-wire and cable impedances, was rejected as it was found impractical to design a transformer which would be adequate over the entire frequency range.

To overcome these difficulties, a new low-capacitance type of cable was developed which could be loaded to match the open-wire impedance with coil spacings about the same as those previously used. Loading coils of different sizes were developed to provide for loading to the different impedances of the open-wire circuits.

The new cable employs 16-gauge conductors in a spiral-four arrangement, supported by hard-rubber disk spacers about 0.6 inch in diameter. These are surrounded by copper and iron tapes for shielding and strengthening purposes. The units so formed may be assembled either in single units in a lead sheath as for lead-in purposes, or in multiple units, up to a maximum of seven for full-sized cable, within the same lead sheath. For duct runs or submarine cables, the multiple assembly is usually employed, and, in the latter case, with outside armoring and jute protection. If the submarine span is more than about 600 feet, intermediate submarine loading is employed.

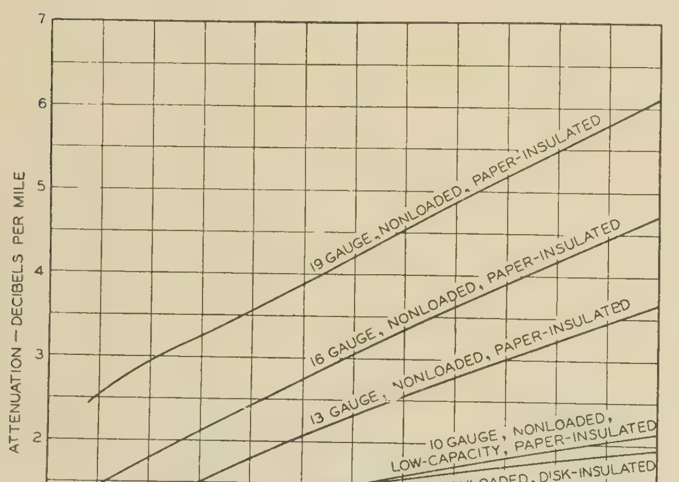


Figure 12. Attenuation of toll entrance cable pairs

As an alternative, it sometimes happens that where a long intermediate cable is involved, an auxiliary type J repeater station can be placed conveniently at one end of this cable. In this case the filter hut described in the discussion of toll entrance arrangements in the next section may be used at the end of the cable opposite the repeater station and the cable treated as a toll entrance cable for the auxiliary office. A further alternative is to provide filter huts at the two ends of a nonloaded intermediate cable. However, if the cable is short, the new disk-insulated cable with loading is to be preferred.

Previous practice at the ends of open-wire lines has been to use paired bridle wire with weatherproof insulation and usually of smaller gauge than the line wire to connect the open-wire pairs to cable terminals mounted on the pole. Other pairs of bridle wire were connected between the open wires and protectors. Because of the much more severe reflection requirements at the higher frequencies of the type J system, these arrangements were no longer satisfactory. The characteristic impedance of bridle wire is roughly one-fifth of that of the open-wire circuit and it has been necessary to avoid the use of even several feet of it between the open-wire and the cable terminal or protectors. To accomplish this, separate terminals for each disk-insulated unit are mounted on the crossarm near the open-wire pairs to which they connect. Four insulated wires from each terminal go by the shortest feasible route to the longitudinal choke coils and protectors and thence to the open-wire pairs.

Toll Entrance Arrangements

The new disk-insulated cable used for intermediate cables was also suited for lead-in or toll entrance cables.

When an auxiliary station is established at a point along an open-wire line where there has not previously been an office, it is usually located close to the line so that the lengths of lead-in cable required are comparatively short. Lengths of this cable up to about 175 feet can be loaded to open-wire impedances with adjustable loading units in the repeater station. For longer lead-in cables up to 300 feet, supplementary loading may

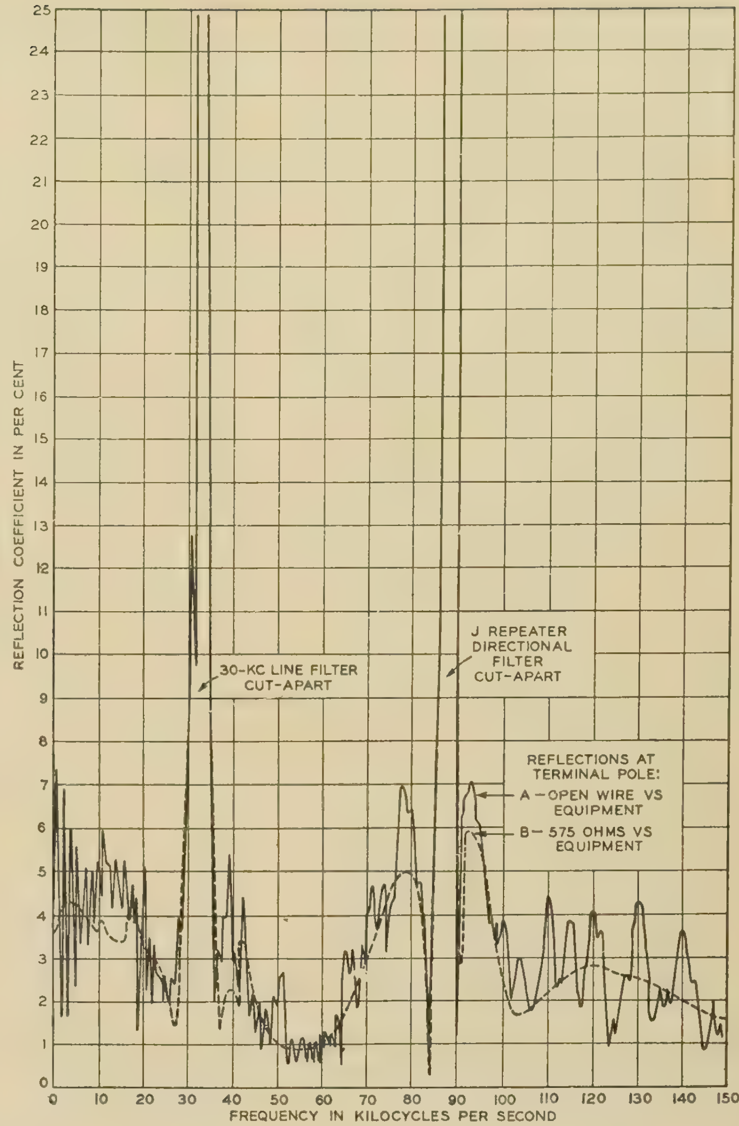


Figure 14 (above). Reflection coefficient at junction between open-wire and toll entrance equipment

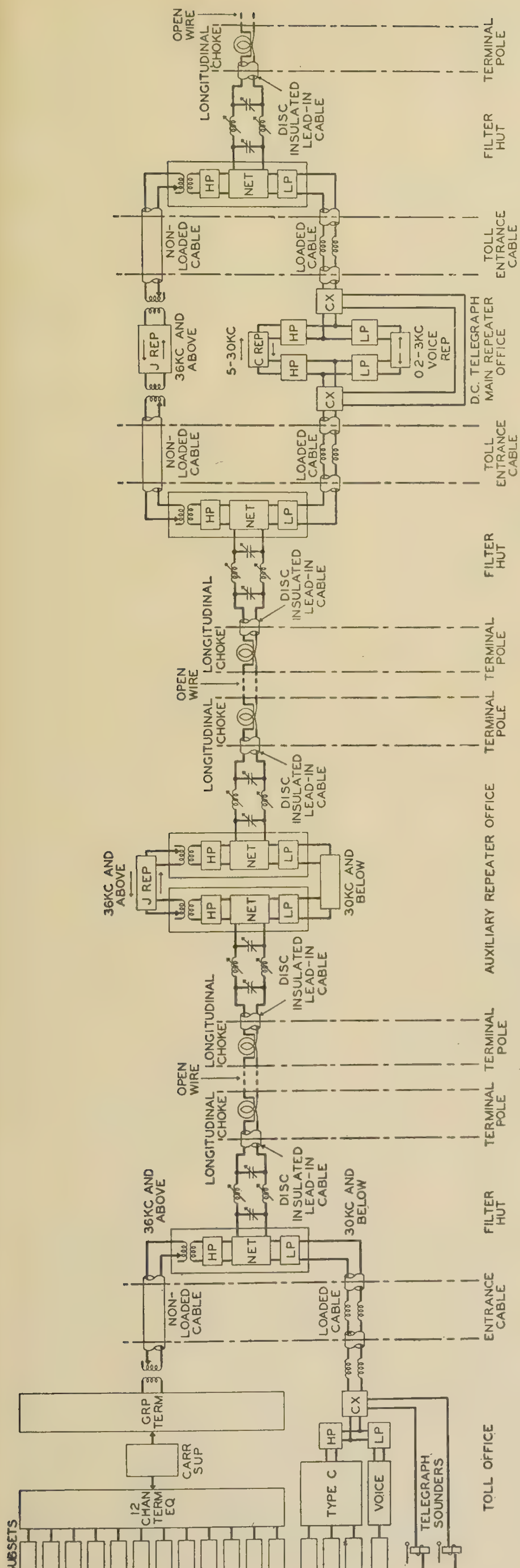


Figure 13. Toll entrance arrangements at J terminal and repeater offices

be mounted directly on the pole at the cable terminals.

When an auxiliary repeater station is not close to the open-wire line, or at main repeater stations which are frequently in towns and separated from the open-wire line by greater lengths of toll entrance cable, it is still possible to use the loaded disk-insulated cable. Because of the cost of this cable and its loading, however, it has sometimes been found more economical to build a hut near the open-wire terminal pole and to separate the type *J* from the type *C* and lower-frequency facilities at that point by means of filters. The connection from the open-wire line to the hut is provided by what is usually a short length of loaded disk-insulated cable. From that point, the type *J* frequencies are led into the toll office over nonloaded paper-insulated pairs while the *C* and lower-frequency facilities are brought in over the existing pairs, usually loaded. By thus limiting the frequencies transmitted over the nonloaded

cable pairs to the J range, it becomes practical to design transformers for suitable impedance matching.

The line filter sets located in the hut are designed for a nominal impedance of 560 ohms which is a compromise for the range of impedances normally found with different wire sizes and spacings. An accurate match with the line is obtained with a building-out network which is adjusted at the time of installation to fit the particular open-wire pair involved. On the office side of this line filter set a transformer provides for stepping down the impedance from 560 ohms to the impedance of the toll entrance cable, which is usually about 125 ohms. Adjustment of this impedance over the necessary range to match impedances of particular cable pairs is provided by means of taps on the transformer. At the office another transformer similarly tapped is employed to match the toll-entrance-cable pair impedance to that of the office wiring.

Figure 12 shows the losses of the commonly used 19-, 16-, and 13-gauge paper-insulated toll entrance cable, a new 10-gauge low-capacity cable, and the new disk-insulated cable. Because of the high losses of the smaller-gauge pairs, it is sometimes economical to place new 10-gauge cable to save repeater costs.

For the office wiring of the J system a rubber-covered shielded pair is used to provide the desired flexibility and freedom from capacitance variation due to humidity changes. Its impedance at 140 kilocycles is approximately 125 ohms. The repeater and terminal high-frequency impedances are designed to match this impedance very closely.

Figure 13 illustrates the arrangement of the toll entrance equipment involved in matching the line impedance to that of the equipment with a minimum of reflection. The terminal is illustrated to the left. The high-frequency line passes to the line filter set which is here shown as located in a filter hut. There it is joined by the type C and lower-frequency circuits and passes through the lead-in cable and protective arrangements on the terminal pole.

Proceeding toward the right in the figure, the arrangement at an auxiliary repeater station is shown. In this case the type J frequencies are amplified in the repeater, but the type C and lower frequencies are by-passed through filters which suppress longitudinal and metallic transmission above 30 kilocycles. At the right is shown a combined type J and type C main repeater office.

Satisfactory cross talk between pairs in entrance and intermediate cables carrying J systems is effected through special selection methods and the application of balancing capacitors.

Reflection Coefficients

The success of the various measures taken to ensure good impedance matching is shown by the curves of figure 14, which are of reflection coefficients measured at an auxiliary repeater station. Curve A, the solid line, gives the coefficient between the open-wire pair and the lead-in cable at the terminal pole. The smaller variations are due partly to irregularities of the open-wire line and, at the lower frequencies, partly to the test terminations at the distant end. The contribution of the cable loading and office equipment is indicated by the dash-line curve B, which was obtained with the open-wire line replaced by its nominal impedance, a 575-ohm resistance. The reflection between the open-wire and toll entrance and repeater equipment is well under five per cent over nearly all of the transmitted range.

Conclusion

The successful transmission of frequencies up to 140 kilocycles over open-wire pairs as compared with earlier operation up to 30 kilocycles has involved modification of the construction of the open-wire lines, new transposition designs, new toll entrance arrangements, including new types of cable, the improvement of impedance matches in various parts of the circuits, closer repeater spacings, and, where ice is encountered, provision for much greater gain margins with more flexible regulation. By the first part of this year about 60,000 channel-miles were in service over type J systems.

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5. Transcontinental Telephone Lines, **J. J. Pilliod.** Electrical Engineering, volume 57, October 1938, pages 418–19 and 423.

6. Some Applications of the Type J Carrier System, **L. C. Starbird** and **J. D. Mathis.** AIEE Transactions, volume 58, 1939, pages 666–74 (December section).

Discussion

L. L. Burns (Southwestern Bell Telephone Company, Dallas, Tex.): The paper by Messrs. Ilgenfritz, Hunter, and Whitman has given a very good picture of the line problems encountered in the development and application of the J system to the telephone plant. It may be of interest to note that it has not been found necessary to use cross-talk suppression devices in paralleling wires on other leads in order to control interaction cross talk at any of the repeater points in Texas. With the advent of the 75-decibel repeater or whenever it becomes necessary to install a second system on any lead such devices may be required.

At several of the J repeater stations in Texas, another pole line parallels the telephone line. These lines by-pass the repeater station and act as a shunt on the cross-talk suppression applied to the telephone lines. If the loss through this shunt path is lower than the gain of the J repeater, singing will occur. Such was the condition found at the Edgewood repeater station when the Dallas-Longview system was cut in service. In this case the gain of the east to west repeater was limited to 33 decibels by singing at a frequency above 150 kilocycles. The installation of a low-pass filter with a cutoff at 150 kilocycles in the J repeater eliminated the singing condition and permitted the full 45-decibel gain of the repeater to be used.

When circuits on other lines offer a shunt path at repeater points the need for suppression measures in these lines will be determined in each particular instance by appropriate measurements, but it is believed that there is a possibility that if any suppression is required, the longitudinal coils alone will be sufficient since the longitudinal coupling is the controlling factor rather than metallic coupling. The loss offered by these coils to the metallic and longitudinal circuit is negligible below 35 kilocycles. The loss to the metallic circuit in the range of 35–150 kilocycles will not exceed a few tenths of a decibel. Each coil will add two ohms to the d-c resistance of each wire.

When metallic coupling is of such magnitude as to become important, it will be necessary to install cross-talk suppression filters in the circuits on other lines. These filters as at present designed are of two types, one with a cutoff at about 12 kilocycles and the other with a cutoff at about 35 kilocycles. The 12-kilocycle filter will offer a loss of 0.5 decibel in the range up to 12 kilocycles, rising to 70 decibels at 140 kilocycles. The 35-kilocycle filter will offer a loss of from 0.2 to 0.7 decibel up to 30 kilocycles, rising to an average value of 55 decibels at frequencies above this range. The loss of both filters in the longitudinal circuit is similar in magnitude to that in the metallic circuit.

The interaction cross-talk problem is not confined to repeater points. Where an east terminal and a west terminal are installed in the same office, the coupling between the outputs of the two terminals may be enough to cause intelligible cross talk. A part of this coupling may be direct between the two circuits and a part may be due to coupling through a third line. This third line might

be a telephone cable, a power line, or signal line which parallels both open-wire leads or cables for a short distance. This condition existed in the Dallas office. It was found in this particular case that the attenuation offered by the relatively long paper-insulated toll entrance cables to frequencies in the J range together with the shielding effect of these cables was sufficient to prevent appreciable cross talk. Longitudinal choke coils may be installed in each circuit in one of the toll entrance cables, if necessary to minimize cross talk of this type. Such coils were installed in one of the Dallas toll entrance cables but subsequently removed when it was found that they were not required.

R. W. Linney (Southwestern Bell Telephone Company, Oklahoma City, Okla.): The authors have given an interesting description of the problems encountered, the methods of solving them, and the results obtained by their application in connection with the installation of the 12-channel carrier telephone systems to open-wire lines. There is a large number of open-wire lines on which the J carrier systems will be installed in the future. The toll fundamental plans will indicate the most probable lines for development with the new carrier systems. On existing lines some of the modifications necessary for the application of the J carrier systems may be made more economically over a period of years before the time the system is required. These lines are constantly being altered on account of ordinary deteriorations and additions and are occasionally rerouted on account of obstructions.

The following is a list of the ordinary construction activities encountered in maintaining an open-wire line which can be directed in a systematic program to effect a reduction in the costs of modifying a line for J carrier operation:

1. *Pole Replacements.* The poles replaced should be located to fit in with the proposed transposition layout. This involves preparation of the transposition schemes in advance.

2. *Long-Span Installations.* Long spans introduce serious problems to J carrier facilities. Consideration should be given to the location of transpositions and to the spacing of wires to meet the requirements of J carriers.

3. *Intermediate and Entrance Cable Installations.* Proposed intermediate and entrance cables with their complicated design should be compared with open-wire reroutes, keeping in mind that the cables at best offer transmission inferior to open wire.

4. *New Wire Stringing Projects.* In placing or replacing wire on a line, consideration should be given to locating it in such a position and spacing it so as to be suitable for carrier operation.

5. *Major Crossarm Replacement Projects.* Ordinarily crossarm replacements are scattered so that a wire respacing job could not be associated. However, in certain cases where a phantom is not in use, it would be practicable to respace the wires at the time a major crossarm replacement job is carried out.

6. *Rerouting Sections of Line.* Where sections of lines are relocated to clear highways and other obstructions, the new construction should be carried out to provide for the ultimate carrier requirements for the section involved. The new route should avoid paralleling other overhead lines especially near proposed J repeater stations to reduce interaction cross talk. Power-line carriers, power-supply lines, and radio stations at close proximity should also be avoided as these systems are a source of noise in J circuits.

A program including some of the items mentioned above has been in effect in connection with applying C and D carrier systems and material savings have been obtained. The more rigid design requirements of the outside plant associated with the application of the higher-frequency carriers offers an even better field of application for such a program.

R. M. Carpenter (Southwestern Bell Telephone Company, Houston, Tex.): The paper very comprehensively presents the many varied and technical problems encountered in the application of high-frequency telephone channels to open-wire telephone lines. Several factors not heretofore offering any appreciable hindrances to commercial transmission must now be given minute study and consideration. Just how well the Bell Telephone Laboratories have done the job of overcoming these obstacles is illustrated by the fact that several J carrier systems are now rendering high-grade telephone service in several parts of the nation under many varied conditions. The authors of this paper played the major role in the development of this carrier and its application to different types of open-wire structures, and deserve much credit for doing an excellent work.

We in Texas are very much interested in the economies offered by the 12-channel open-wire carrier system as a means of providing much of the additional toll facilities required to keep pace with the increasing usage of long-distance telephone service. This carrier system is particularly adapted to providing many additional miles of high-frequency communication channels from the large number of existing long-haul toll circuits in the Texas area. Most of the major toll lines in Texas consist of open-wire structures and there are relatively great distances between the larger toll points. An extended use of such systems seems feasible in Texas not only because of the above features, but also for the reason that the conditions contributing to line problems are not as severe as in most parts of the nation.

Firstly, deposits of ice on wires in Texas, as a whole, are not a frequent occurrence. The picture of the ice on wires and insulators near Amarillo, Tex., does not lend any strength to this statement, but, those of you who are acquainted with the storm-loading areas, know that only a small portion of Texas is in the heavy-loading area, where, for pole-line-design purposes, heavy ice may be expected at certain intervals. A somewhat larger section lies in the medium-loading area where a lighter coating of ice may be had at rare intervals, but by far the greater portion is in the light-loading area where no ice is considered for design purposes. In deference to the picture, it should be said that ice will form more frequently on the wires in the colder Panhandle section of Texas, but, in the rest of the state, in the heavy and medium areas, sleet will not be a factor more than once or twice each year, and, in the light-loading area is a rarity. Therefore, deposits of ice which greatly attenuate the high frequencies cannot be considered a major problem in most parts of Texas.

Secondly, the toll lines in Texas will have shorter lengths of entrance and intermediate cables in their make-up than will those in more densely populated sections of the country. In the 265-mile line between Dallas and Houston, there is not more than 11 miles of cable, and, in the 290-mile Dallas-San Antonio line, the cable mileage is proportionately low. Inasmuch as cable inflicts a severe penalty on the higher frequencies of this open-wire carrier system and requires costly loading as well as special types of conductors in some instances, it is readily seen that the less cable there is in a toll line, the greater economy there is in placing J carrier systems on the line.

Thirdly, external sources of noise will not be as great as in some sections of the country. Dust storms occur frequently in the Panhandle and other parts of west Texas, and, in recent years, some of these have been blown eastward into central and east Texas, three to five times each year, but, in general, the portion of the state having most of the major toll lines is affected little or none by dust storms. The relatively low density of population in Texas serves to keep to a minimum such external sources of noise as radio stations, power-line carrier, and power-line supply systems.

Texas has been fortunate in securing most of the early pioneering installations of J carrier systems, and, in view of the above favorable conditions, will, no doubt, find it economical and practicable to make an extended further use of this type of communication facility.

Some Applications of the Type J Carrier System

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MEMBER AIEE

J. D. MATHIS
ASSOCIATE AIEE

Synopsis: Previous papers before the AIEE describe the development of a 12-channel type *J* carrier system. This paper discusses some of the practical problems encountered in extending the circuit capacity of existing open-wire lines by the use of this carrier system.

The first systems of this type were placed in commercial operation late in 1938. One of these systems is discussed in detail from the standpoint of obtaining satisfactory operation with the most economical arrangement of new and existing facilities.

A 12-CHANNEL carrier telephone system for open-wire lines was described before the AIEE early this year,[1] and a discussion of the requirements of line facilities for its operation has been presented.[2] Since the first three systems to be placed in commercial operation are located in Texas, it seems appropriate to present to the South West District meeting the major problems arising from the practical application of this type system on existing open-wire plant.

In 1935 it became apparent that existing open-wire facilities on some of the major toll lines in Texas would soon be exhausted. In the case of the Dallas-Houston, Dallas-San Antonio, and Dallas-Longview lines, current growth and requirements for the future indicated that comprehensive relief would have to be provided ultimately. This probably will be a toll cable. At the time, however, the development of the open-wire 12-channel *J* carrier system made available an arrangement for obtaining a large number of additional circuits over the existing lines which would provide for the immediate requirements and also permit postponement of more costly relief measures for a number of years.

The type *J* system operates in a frequency range above that of the three-channel type *C* carrier system and can be superposed on the same conductors with the type *C*, thereby providing a total of 16 circuits from one pair of conductors. However, conductors suitable for type *C* carrier operation are not necessarily satisfactory for the operation of the new system.

The three lines under consideration were practically of the same construction, being 12-inch phantomed lines originally built for voice-frequency circuits only and later modified for the application of type *C* carrier systems. Over lines of this type, it is practicable to operate a single type *J* system without any material change in the line wire because no crosstalk considerations are involved, although it is necessary to select by transmission measurement pairs which are free from absorption effects. Where more than one system is required a transposition arrangement has been designed for use with line conductors of a nonphantomed pair spaced 6 inches apart and 30 inches between conductors of horizontally adjacent pairs. This design can be used either for new wire or for existing wire retransposed, and can be applied without regard to the existing phantomed transposition design, thereby permitting respacing and retransposing any portion of the existing wire, a phantom group at a time if desired.

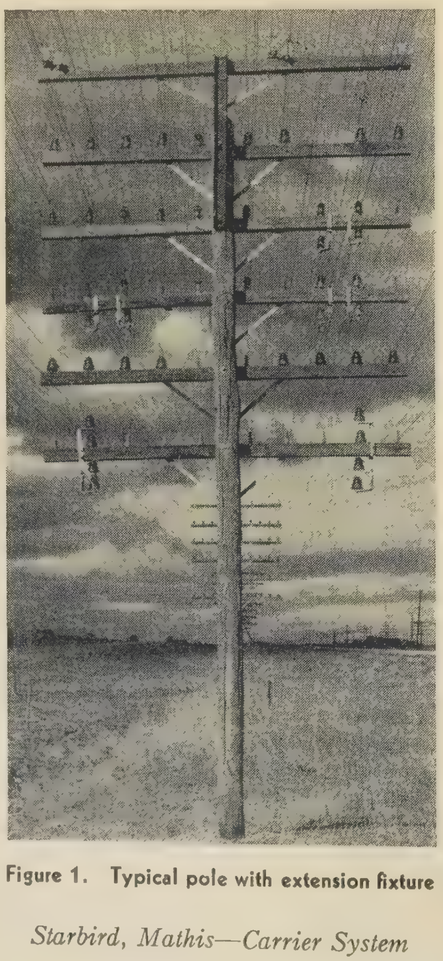

Figure 1. Typical pole with extension fixture

Advance Engineering

With these operating limitations a review of the circuit requirements established a plan to place a *J* carrier system on one of the phantom groups of the Dallas-Longview line during 1938. This system would not only provide sufficient circuits to meet the additional requirements but would furnish sufficient spare circuits to release one phantom group of 12-inch wire for respacing and retransposing. This plan was not applicable to the Dallas-Houston and Dallas-San Antonio lines since circuit relief was required for the 1937 business, and the *J* carrier system would not be available until 1938. These lines each consisted of five crossarms of 104-mil wire over the greater portion of their length. An inspection showed that, although the poles

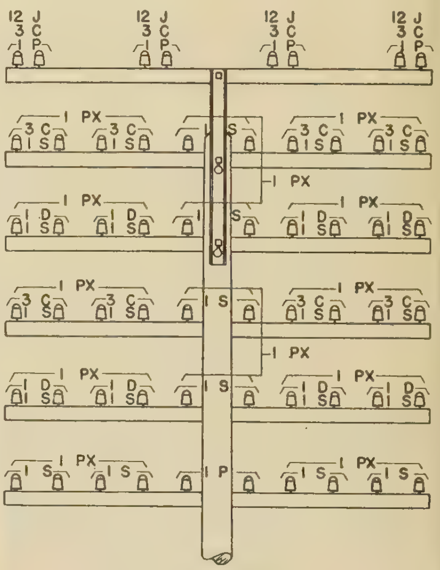

Figure 2. Pole head diagram showing circuit capacity of the Dallas-Houston and Dallas-San Antonio lines

P—Physical circuit
PX—Phantom circuit
S—Side circuit
C—*C* carrier circuit
D—*D* carrier circuit
J—*J* carrier circuit

Paper number 39-79, recommended by the AIEE committee on communication, and presented at the AIEE South West District meeting, Houston, Tex., April 17-19, 1939. Manuscript submitted January 30, 1939; made available for preprinting March 9, 1939.

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1. For all numbered references, see list at end of paper.

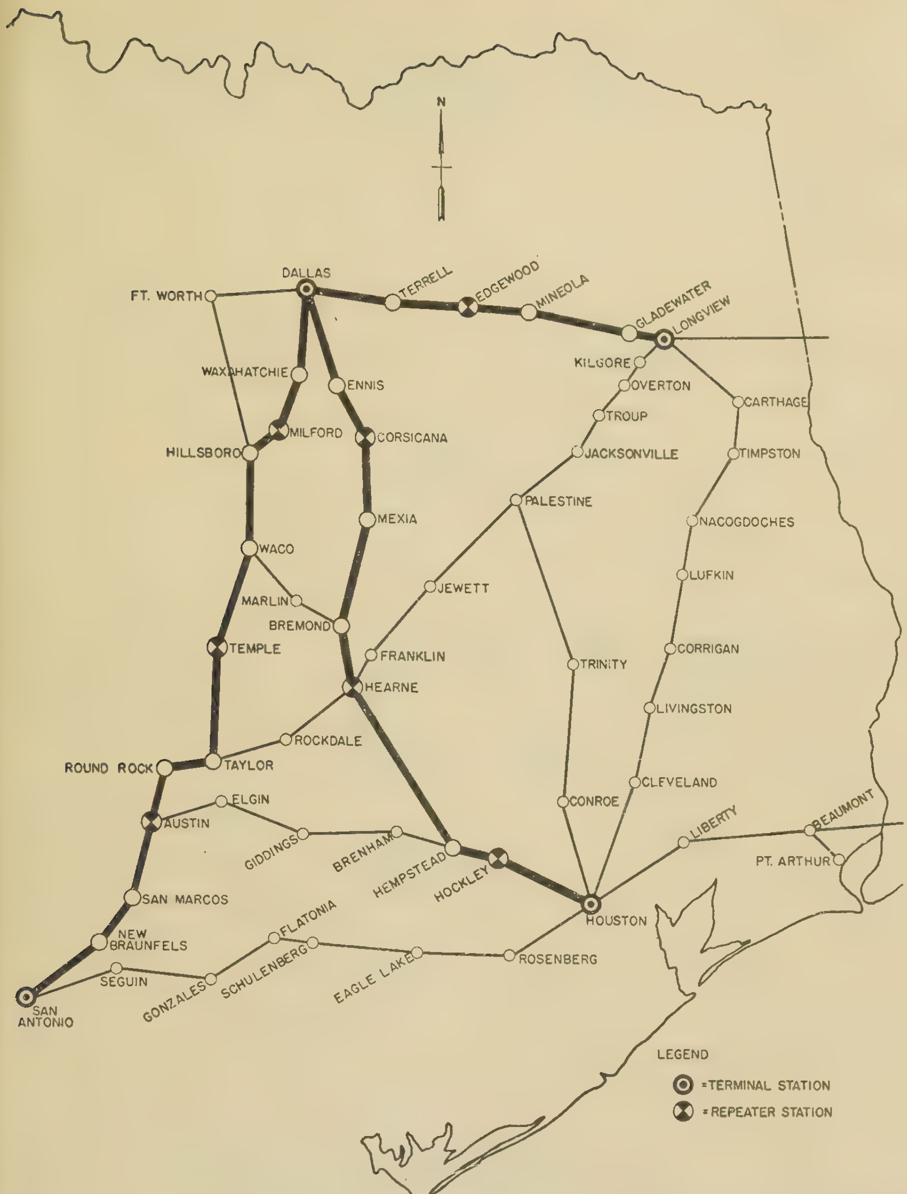


Figure 3. Routes of toll lines on which the J carrier systems are applied

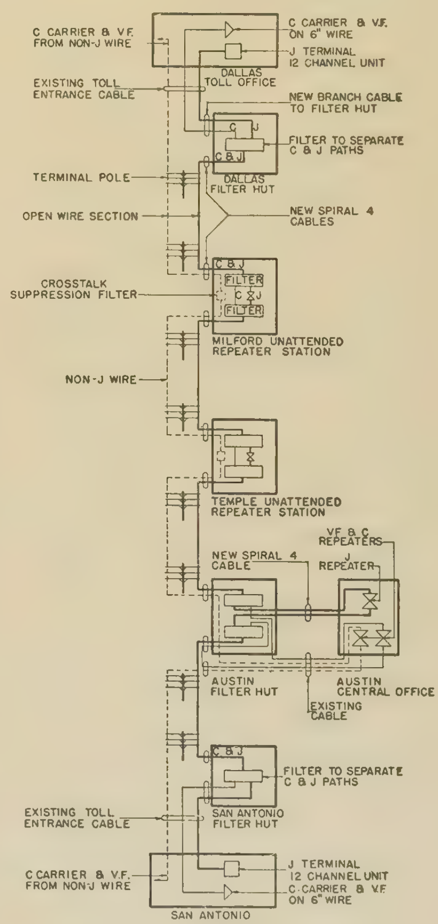


Figure 4. Arrangement of facilities for a typical J carrier system

were of sufficient strength to support additional crossarms, it would be difficult to maintain the necessary wire clearance with an additional crossarm below the existing wire and also that new wire so placed would be susceptible to interference from possible breaks in the wire above.

The solution of this problem was the addition of a crossarm two feet above the others on a simple extension fixture. This fixture shown in figure 1 consists of a four-inch steel H beam fastened to the pole by the through bolts which also support the two upper crossarms. By placing four pairs of six-inch-spaced conductors on the new crossarm and by using four type *C* carrier systems, 16 additional circuits were obtained to furnish the circuit relief for 1937 and, in addition to the immediate relief, four suitable *J* carrier paths were provided of which one on each line was needed in 1938. Figure 2 is a typical pole head and shows how the ultimate circuit capacity of this open-wire plant has been expanded from 69 to 133 circuits by the addition of one crossarm and eight conductors. The use of 128-mil wire instead of 104-mil wire provides greater strength and the particular location reduces the probability of interrupting 16 circuits by a single wire break or other physical interference.

The program of placing three type *J* carrier systems in service in Texas during 1938 was established. Figure 3 is a map of a portion of the state showing the routes of the lines and the principal cities along the routes. Since the length and attenuation of each of these lines are such that the carrier systems cannot operate without intermediate amplification, it was necessary that the number and locations of repeater stations be determined.

Typical System

The layout of a particular system is largely controlled by available repeater gain, existing entrance cables, line attenuation under normal and adverse weather conditions, location and availability of existing telephone buildings, and availability of commercial power for new buildings. Line attenuation is increased greatly by deposits of ice on the wire during sleet conditions. Although data are available regarding the frequency of large deposits of ice, there is very little information as to the amounts or frequency of occurrence of small deposits. Under normal wet-weather conditions the maximum attenuation of six-inch-spaced 128-mil facilities at 140 kilocycles is 0.35 decibel per mile.

On the Dallas-San Antonio line the facilities available consisted of 286 miles of six-inch-spaced 128-mil copper wire and 42,000 feet of 16-gauge nonloaded paper-insulated cable. Using repeaters having a maximum amplification of 45 decibels in

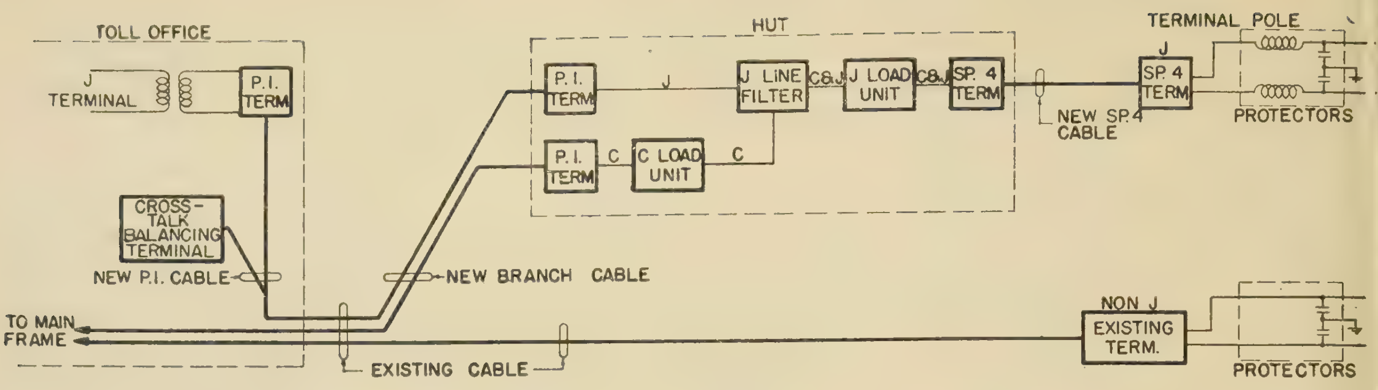
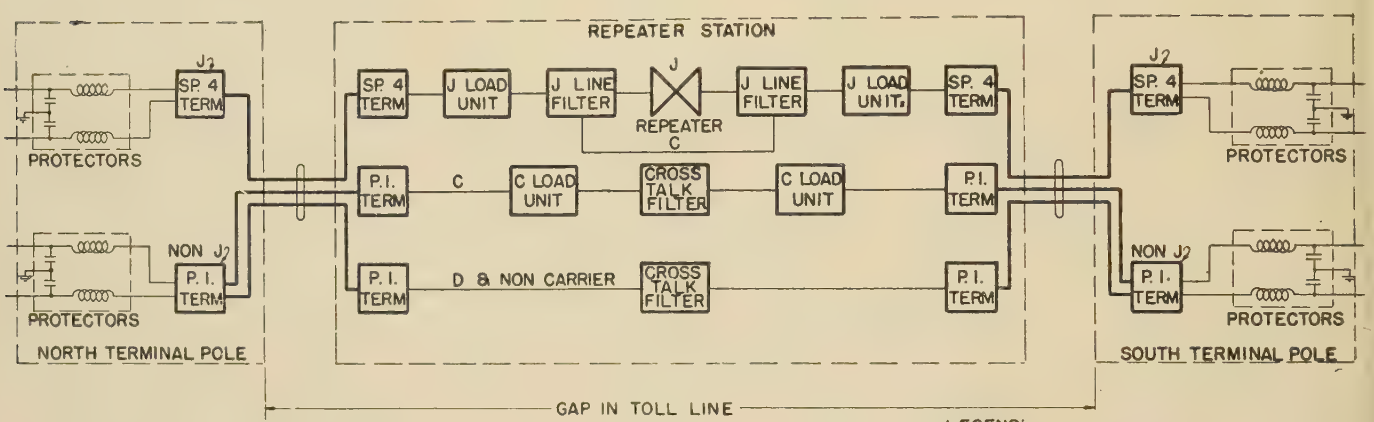


Figure 5. Circuit connections through Dallas filter hut from open wire to terminal equipment at central office

LEGEND:
P.I. = PAPER INSULATED
SP. 4 = SPIRAL FOUR
TERM. = CABLE TERMINAL
(coil symbol) = LONGITUDINAL RETARD COIL



LEGEND:
TERM. = CABLE TERMINAL
SP. 4 = SPIRAL FOUR
P.I. = PAPER INSULATED
(cable symbol) = CABLE
(coil symbol) = LONGITUDINAL RETARD COIL

Figure 6. Circuit connections through Temple unattended repeater station

each direction of transmission, the provision of two intermediate repeaters would provide sufficient gain to take care of the wet weather conditions with no extra margin; three repeaters would provide 45 decibels margin and four would provide 90 decibels margin for the over-all system. Considering the location of this line and the small probability of obtaining large deposits of ice in accordance with past experience, it was decided to select tentatively three intermediate repeater stations which would provide sufficient gain to take care of attenuation up to about 0.5 decibel per mile as compared to the wet-weather value of 0.35 decibel per mile.

For type *C* operation over this line, only one type *C* carrier repeater point is required, and it is at Austin. Considering the availability of power equipment and operating personnel and the possibility of future *J* carrier terminals being located at Austin, it is desirable that this be one of the repeater points on the *J* system. A division of the attenuation of the facilities north of Austin indicated that the other stations should be in the vicinity of Temple and Milford.

At these repeater stations amplification is needed only on the type *J* system and the other circuits on the line pass through these stations without amplification. Under these conditions energy may be transferred from the output of one type *J* repeater to the input of the same repeater or to the input of a repeater on another *J* system via cross-talk paths involving the wires which are not used for type *J* systems. The effect of this transfer of energy is accentuated by the fact that there is a large difference in transmission level between the output of one type *J* repeater and the input of the same or another repeater. In order to minimize these effects it is necessary that all wires on the line be given special treatment, including a gap in the toll line, longitudinal choke coils in all wires at terminal poles, and cross-talk suppression filters in the non-*J* pairs in the repeater station itself. In selecting locations for repeater stations, consideration must also be given to the possible coupling between type *J* systems by interaction paths involving other conductors adjacent to the toll line.

Before definite selection of repeater station locations may be made, it is necessary that each repeater section be checked in detail and in this check the entrance cable arrangement may be controlling. The newly developed spacer-insulated spiral-four cable, either loaded or nonloaded, or nonloaded pairs of the conventional paper-insulated cable may be used between the open wire and equipment. Generally the existing voice and *C* carrier circuits use loaded entrance-cable pairs and in most cases a change to nonloaded facilities would require extensive rearrangements in these circuits. In order to use nonloaded pairs for the *J* carrier and leave the *C* carrier and voice on loaded facilities, filters are placed at the terminal pole to separate the *J* carrier frequencies from the *C* and voice frequencies at that point. A limitation on the use of existing cable is that suitable pairs must be selected by cross-talk measurements and balanced at 140 kilocycles to meet the requirements of the system. The paper-insulated conductors have

the largest attenuation of any of these facilities, and the loaded spiral-four the least. The various entrance arrangements from the open wire to the office equipment are described in more detail elsewhere.[2] The choice of the facility used in any particular case will depend upon the resultant over-all economy.

The large number of nonloaded pairs in the existing 1.8-mile entrance cable at San Antonio indicated that sufficient pairs could be selected which would be satisfactory from the cross-talk standpoint for *J* carrier operation. Six pairs were subsequently selected and balanced.

At Austin a single toll entrance cable, one mile in length, with two complements, terminates the line from the two directions. Although the two complements are separated by a layer shield, this cable is not suitable from a cross-talk standpoint for operation of the *J* carrier in and out of the office; therefore, at least one additional cable is required from the central office to the toll line. For this purpose a new nonloaded spiral-four entrance cable was indicated for the type *J* system with the type *C* and voice circuits continuing to use the existing cable. The separation of the type *J* circuits from the non-*J* circuits on the same pairs is accomplished by filters which are located in a small building at the junction of the toll line and the entrance cables. The use of a single entrance cable for the non-*J* wire in both directions on the telephone line indicated that it might be necessary in the future to use cross-talk suppression filters at this point. Accordingly, the filter hut was made large enough to include future cross-talk suppression filters if required as well as the line filters which separate the type *J* from the non-*J* circuits.

Figure 7. Arrangement of gap in toll line at unattended repeater station

A repeater station at Temple could have been located in the existing central office or could be located in a separate building in or near the city. In either case a new power plant was needed since the existing plant could not be economically modified to serve the *J* carrier repeaters. The telephone line is continuous through the city, only those wires used for Temple circuits being terminated in the office through one entrance cable 0.6 mile in length. This cable is not suitable for *J* operation in both directions, which would require one additional cable if the repeaters were located in the central office. Numerous signal and supply lines in proximity with the telephone line within the city offered interaction crosstalk complications. A separate repeater station near the toll line in or near the city avoids the placing of a long entrance cable, reduces the over-all system attenuation, and eliminates the problem of interaction crosstalk from paralleling lines. Other factors including cost showed very little difference between a separate station and placing the repeaters in the central office. An unattended station near the toll line was indicated.

A common entrance cable at Dallas terminates the wire on both the Houston and San Antonio lines, the terminal of the Houston line being 2.9 miles from the central office, and of the San Antonio line one mile further. This cable previously had been placed in three different sections, each section having a different make-up, and there was considerable doubt as to the number of suitable pairs for *J* operation that could be obtained. The use of either a loaded or nonloaded spiral-four cable would not improve attenuation sufficiently to change the number or materially alter the locations of the repeater stations from those tentatively selected, but would provide some additional margin for sleet conditions. The expense of loading the spiral-four cable, if placed, could not be justified by the improvement in over-all attenuation. Using either nonloaded spiral-four or existing nonloaded paper-insulated conductors requires filters at the open-wire terminus. With these considerations, it was decided that suitable pairs would be used in the existing cable until exhausted. Subsequent cross-talk selection tests have indicated that 12 pairs, 6 for each line, are available.

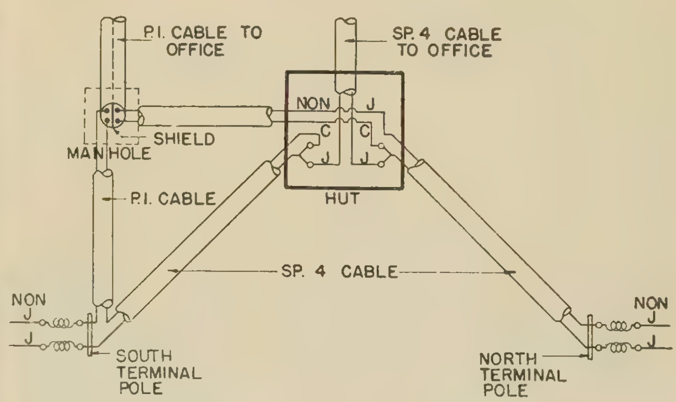


Figure 8. Cable arrangement at Austin

SP.4—Spiral four
P.I.—Paper insulated

Since there was no suitable central-office building at Milford, the repeater station in that vicinity must of necessity be in a new building preferably near the toll line. Commercial power is available only near the town, forcing a tentative location to be selected at the edge of the city.

With these selections of entrance-cable facilities and tentative repeater-station locations, the distribution of gain and line loss by repeater sections is shown in table I. A satisfactory distribution of line loss has been obtained and an analysis of these data shows that further improvement is impracticable. Therefore, the tentative repeater-station locations were adopted.

Figure 4 is a diagram of the major line and equipment parts of the Dallas-San Antonio lead. The *J* carrier path is shown by heavy solid lines, the *C* and voice on the same wire with the *J* by light solid lines, and all other circuits, classed as non-*J*, by dotted lines. Figures 5 to 7, inclusive, show in more detail the arrangements at the huts and repeater stations. The figures for the Dallas hut

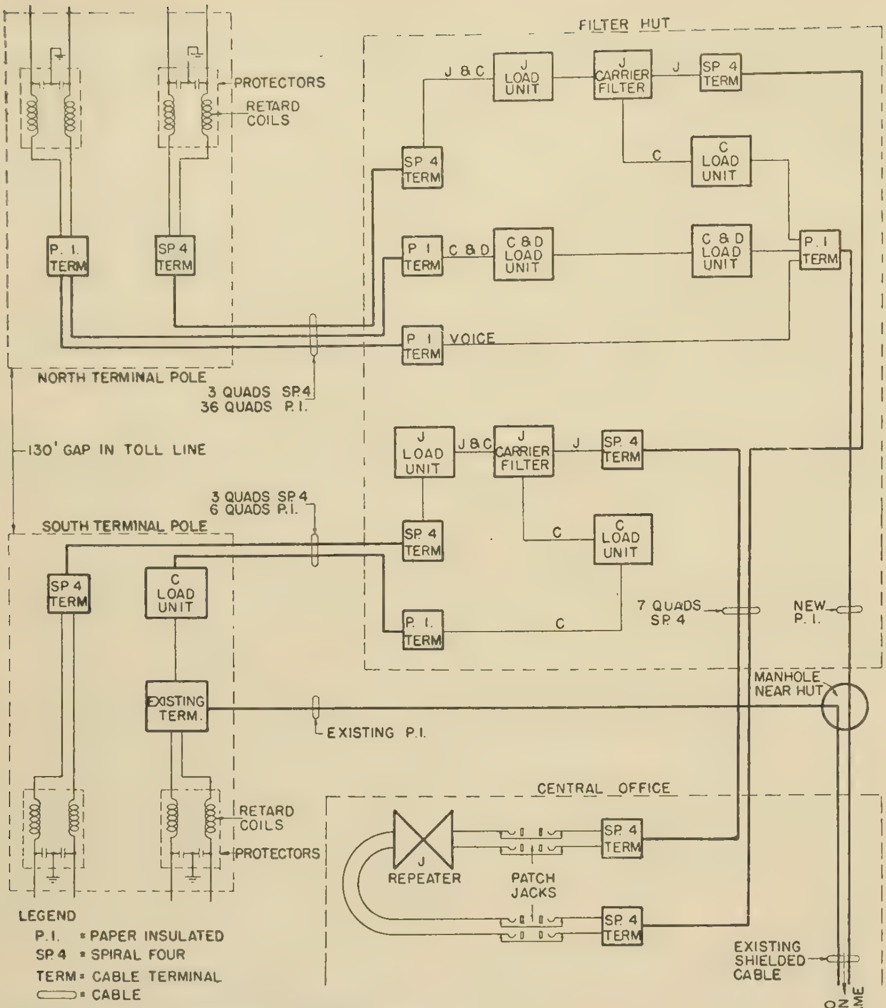


Figure 9. Circuit connections through Austin

and Temple repeater station are typical, and huts and unattended repeater stations not shown differ from these only in minor details. It will be noted that all wire on the toll line is brought through the repeater stations while only that wire on which *J* carrier is superposed is routed through the huts except at Austin where all wire to the north is brought through the hut to allow the future application of cross-talk suppression filters if required. For both huts and unattended repeater stations, short lengths of loaded spiral-four conductors are used from the six-inch-spaced wire at the terminal poles to the equipment in the buildings. A single continuously adjustable load unit is used for each pair and is located with the equipment. Paper-insulated pairs under the same cable sheath as the spiral-four conductors are used for the non-*J* wire.

As previously mentioned, the conditions at Austin were complicated by a single cable for existing circuits and a new cable for *J* carrier in both directions. Figure 8 is a diagram of the existing and new cables to the filter hut and terminal poles, and figure 9 shows the interconnection of circuits and equipment used at the filter hut, terminal poles, and central office.

Terminal and repeater equipment in existing offices is located in space adjacent to other equipment terminating toll circuits, and makes use of the common office equipment and power plant.

The new repeater stations and the filter huts are arranged for unattended operation. The equipment in the filter huts is such that no adjustments or attention are required other than periodic inspections. In the unattended repeater stations the power-supply equipment is automatic in its operation. Although periodic maintenance attention is necessary, it is desirable that any abnormal condition be recognized as soon as practicable and a system of alarms has been provided from each unattended station to an adjacent main repeater or terminal office. This alarm has been arranged to operate by direct current over one conductor between offices without interfering with existing telephone circuits but at the expense of one d-c telegraph path. For fuse failure, rectifier failure, power off, power restored, high-low voltage, high-low temperature, fire, burglary, pilot channel failure, and end of pilot channel control, alarms are sent and identified. A questionable alarm may be rechecked from the attended office.

Special Problems

Some of the problems encountered in connection with the other two systems may be of interest. At Corsicana, a repeater point on the Dallas-Houston system, a filter hut was used on only one side of the repeater station. The situation which led to this arrangement is that an intermediate cable in the Dallas-Houston line extends 0.2 mile north and 0.5 mile south from the local central office. As it is necessary that the *J* system operate through this entrance cable and since space was available in the local central-office building, repeater equipment similar to that installed in unattended buildings was placed in one room in the office.

The section of cable north of the central office terminates on a corner in a business district with all adjacent property occupied by buildings, making it more economical to use loaded spiral-four cable to this location rather than extend the existing cable to an available site and provide the necessary filter hut and equipment. For the longer cable, it was more economical to provide the filter equipment in a hut in order to use existing facilities. Although this cable terminates in a fully developed residential area, a site for a filter hut was obtained adjacent to an alley in the rear of one of the residences facing the street on which the terminal pole is located.

The use of nonloaded paper-insulated pairs in existing entrance cables has been mentioned. However, it is in general not practicable for cross-talk reasons to use all the nonloaded pairs which are available in one cable, and the selection of pairs suitable for type *J* operation is illustrated by a discussion of the methods used on the Dallas cable. The Dallas cable is composed of three sections of different make-up. The section nearest the central office, 1.3 miles long, and the intermediate section, 1.6 miles long, each contained 22 idle nonloaded pairs, and the third section, one mile long, had only 6. The Houston line terminates at the end of the second section, the third section extending the cable to the San Antonio line.

Since the number of cable pairs to the San Antonio line was limited to a maxi-

Table I. Distribution of Gain and Loss by Repeater Sections

Repeater Section	Cable Length (Miles)	Cable Loss (Decibels)	Open Wire Length (Miles)	Open Wire Wet Weather Loss (Decibels)	Maximum Tolerable Attenuation in Decibels per Mile at 140 Kilocycles Using 45-Decibel Repeaters	Maximum Tolerable Attenuation in Decibels per Mile at 140 Kilocycles Using 75-Decibel Repeaters
Dallas-San Antonio system						
Dallas-Milford	3.9 miles 16 gauge	17.50	49.2	16.4	0.560	1.165
Milford-Temple	Nominal	Nominal	84.7	27.3	0.532	0.885
Temple-Austin	1.1 miles spiral-4	2.20	74.4	24.8	0.575	0.980
Austin-San Antonio	1.1 miles spiral-4; 1.8 miles 16 gauge	10.30	78.5	26.2	0.635	0.825
Dallas-Houston system						
Dallas-Corsicana	2.9 miles 16 gauge	13.20	52.5	17.5	0.610	1.175
Corsicana-Hearne	0.5 miles 16 gauge	2.25	90.5	30.2	0.484	0.805
Hearne-Hockley	Nominal	Nominal	85.6	28.6	0.545	0.875
Hockley-Houston	5.6 miles 16 gauge	25.20	29.1	9.7	1.190	1.700
Dallas-Longview system						
Dallas-Edgewood	1.4 miles 16 gauge	6.30	55.3	21.0	0.984	1.240
Edgewood-Longview	0.5 miles 16 gauge	2.30	69.7	26.5	0.614	1.040

mum of six and since the rate of circuit growth over the two lines was expected to be approximately the same, requiring cable relief over the entire distance when the branch to the San Antonio line was exhausted, an objective of six pairs to each line was set up.

Measurements of cross-talk coupling at 140 kilocycles in terms of inductance and capacitance unbalance were made between each pair and all other pairs in each section and the pairs rated in their order of desirability. It is of interest that this required a total of 854 measurements. Those pair combinations whose coupling of the mutual-inductance type was high were rated as the least desirable. This was done because capacitance balancing was to be used to obtain cross-talk reduction. The more desirable pairs in the first two sections were connected through to the six pairs in the last section by cut-and-try method until the over-all condition was such that all six pairs were acceptable. By a similar procedure, using the remaining pairs in the first two sections, six pairs to the Houston line were made acceptable. No record is available as to the number of tests made in the cut-and-try process.

A cable terminal on which balancing capacitors were mounted was installed in the central-office building and connected to the selected pairs. This terminal contained 66 small adjustable wire-wound capacitors which were connected between each pair and every other pair. The capacitors were adjusted to reduce to a minimum the capacitance component of the cross-talk coupling.

Buildings

For the three *J* carrier systems, four new repeater stations and eight filter huts were needed. The same type of construction was used for all: concrete foundation with floor slab above grade, double four-inch brick walls with rock-wool insulation between but with solid brick at corners and openings, pitched roof with wood framing, fire-resistant wallboard ceiling, fire-resistant composition shingles, and heat insulation above ceiling and below floor slab.

All of the racks for equipment in the unattended repeater stations are arranged in three rows with power, repeater, and line equipment in separate rows within a floor space of 17 feet by 16 feet which will allow the ultimate installation of six repeaters in each building. The entrance cables from the terminal poles enter from iron conduit through the floor and are racked and spliced on the side wall adjacent to the line bays. The stubs from the cable terminals at the top of the line bays are carried overhead to splices on the wall. A ceiling height of 13 feet is maintained above the equipment but reduced along the pitch of the

Figure 10. Equipment in unattended repeater station

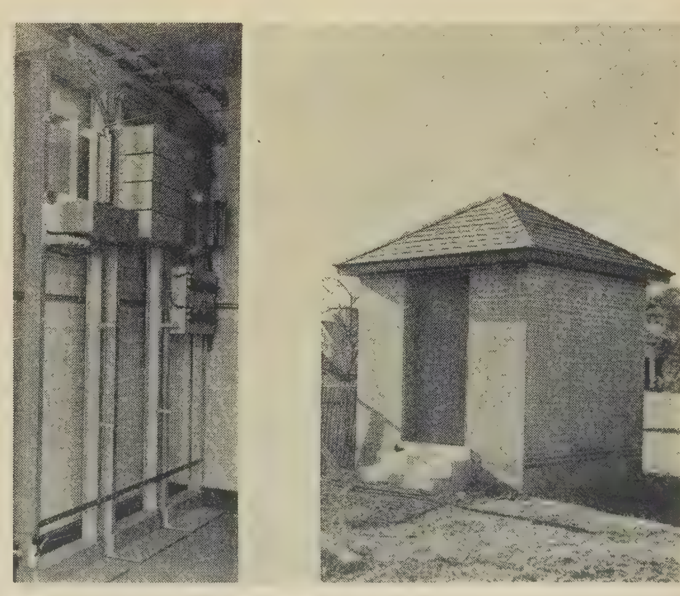


Figure 11. Typical hut and filter equipment

roof to 11 feet 8 inches at the side walls.

For all huts except that at Austin, three adjacent bays of racks are needed. With these along one side wall of the hut, the opposite side is available for splicing the entrance cable. At Austin an ultimate of nine racks, for filters in both directions of transmission, led to the use of racks along opposite sides of the hut with a splicing pit under the floor made accessible by trap doors in the floor between the lines of racks. In this case, the cable terminals are installed at the bottom of the racks with their stubs dropped directly through the floor slab into the splicing pit. The racks in the hut are seven feet high and a ceiling height of eight feet is used. Figures 7, 10, 11, and 12 are pictures of a typical repeater station, typical filter hut, and the special hut at Austin.

For correct operation of the equipment, temperature limits of 32 to 110 degrees Fahrenheit are desirable. Also, it is necessary that there be no precipitation of moisture on wiring or equipment. To maintain the desired conditions, each of the huts is equipped with a two-kilowatt blower-type electric heater arranged to operate at low temperature or high relative humidity, but with operation blocked when the temperature reaches 95 degrees. Each new unattended repeater station is equipped with a four-kilowatt heater similarly controlled and, on account of the heat dissipation of power plant and vacuum tubes, also has forced ventilation which is operative under conditions of high temperature. The system of forced ventilation consists of spun-glass intake filter, exhaust fan, electric-solenoid-controlled shutters at intake and exhaust, and thermostat, and is interconnected with the office alarms to prevent fan operation in case of fire.

Conclusion

Upon completion of the buildings, equipment installation, and line-facility rearrangements, adjustments in the equipment were made to match the lines used. Networks associated with the terminal and intermediate amplifiers were adjusted so that the amplification for any particular frequency would be equal to the attenuation at that frequency in the preceding repeater section; the automatic pilot channel equipment[1] compensates for attenuation changes. In repeater sections containing long toll entrance cables, it was necessary to sacrifice range of automatic pilot channel control to obtain the best equalization. However, satisfactory equalization and range of pilot channel control were obtained in every case.

As mentioned previously, the Dallas-Longview system operates on 12-inch spaced phantomed wire. In figure 13 the attenuation characteristics of two possible pairs are shown. The absorption peaks of pair *B* at 92 and 127 kilocycles are within the frequency range of channels number 4 and 12 of the *J* system and would impair the quality of those channels if pair *B* were used. Therefore, pair *A* is used as the regular path for the system. The quality of the channels obtained from these systems is shown by figure 14. Curve *A* is representative of that obtained from a system operating over six-inch-spaced wires; *B* and *C* are the best and poorest obtained from the Dallas-Longview system.

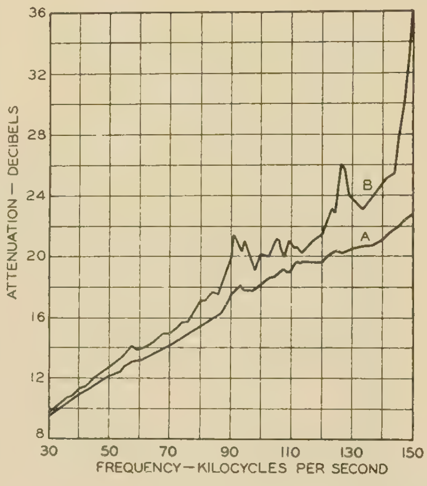


Figure 13. Attenuation of two 12-inch-spaced phantomed pairs of the Edgewood-Longview repeater section

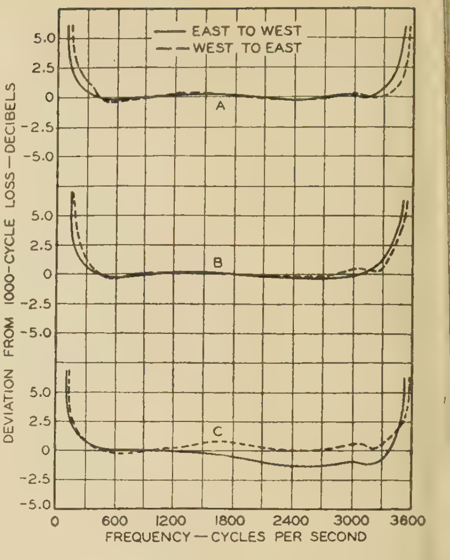


Figure 14. Quality of derived circuits

A for typical channel of systems on six-inch-spaced wire, *B* and *C* for the best and poorest channels of system on 12-inch-spaced wire

The Dallas-San Antonio, Dallas-Longview, and Dallas-Houston systems were placed in commercial service in September, October, and November 1938, respectively. Experience with these systems is that the circuits obtained compare favorably with those obtained from any other facilities in quality and continuity of service, and that a definite need has been fulfilled in providing an economical method of increasing the capacity of the existing plant.

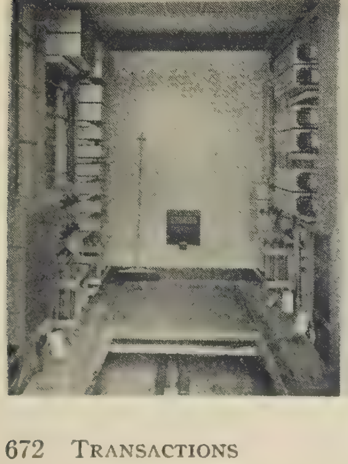


Figure 12. Austin hut and filter equipment

References

1. A 12-Channel Carrier Telephone System for Open-Wire Lines, B. W. Kendall and H. A. Affel. AIEE Transactions, volume 58, 1939, pages 351–60 (July section).

2. Line Problems in the Development of the Type J Open-Wire Carrier System, L. M. Ilgenfritz, R. N. Hunter, and A. L. Whitman. AIEE Transactions, volume 58, 1939, pages 656–65 (December section).

Discussion

Earl W. Lipscomb (Western Union Telegraph Company, Dallas, Tex.): In view of the practice of loading cables for use in voice and carrier circuits up to about 30 kilocycles, it is somewhat striking to note that loading is not used for the *J* frequencies, although greater normal attenuation and greater loss from impedance mismatch

would be expected at these higher frequencies. The necessity for closely spaced loading coils at J frequencies may make loading prohibitive, but it would be interesting to know of the measures used to minimize high-frequency losses in standard nonloaded paper-insulated cable.

Treatment is prescribed for wires on the line which are not used in J systems to prevent their coupling the high output of the J repeaters back into the input of these repeaters. Similar difficulty may be experienced from wires on adjacent pole lines owned by other companies. While it may be possible to apply the same treatment to the foreign-owned wires and lines, it would be of interest to know something of the losses these measures would introduce into the foreign circuits. It appears that the problem may become more difficult in the event that the other companies desired to use similar high frequencies on their circuits. Field tests during the installation of the J systems probably have indicated a separation between lines beyond which the coupling effect need not be considered.

T. L. Keathley (Southwestern Bell Telephone Company, Houston, Tex.): In the paper so ably presented by Messrs. Starbird and Mathis on the subject: "Some Applications of the Type *J* Carrier System" you were told that four circuits of 0.128 copper wire were placed on pole-top extension fixtures on the Dallas-Houston and Dallas-San Antonio lines. It was also pointed out that the two wires forming a circuit were only six inches apart and transposed on point side transposition brackets at an average interval of 260 feet or on every other pole in the line. Several unusual and interesting construction features were involved in the operation of placing this wire.

The crossarm positions on a pole line are termed gains, and are numbered from the top downward. Incidentally, this will explain the derivation of the term "zero gain" which has been applied to the arm on the pole top fixture. Since the existing top arm is designated as the first gain, anything above that must have a lower number, and zero has been selected. Ordinarily the crossarms and wire are added to a pole line from the top downward; but this order is reversed in the case of placing wire in the zero gain. The new wire must be placed above the working circuits and it is very important that it be kept clear of the existing wire during the placing to avoid service interruptions. This is done by placing what is known as an extension side arm fixture on each of the arms on which the new wire is to be placed. This side arm is a two-inch by four-inch piece of timber three feet in length with four notches at one end to engage the new wire. At the other end and also at an intermediate point, this side arm is equipped with S-shaped strap-iron brackets, which hook under and over the arm that is to carry the new wire, the notched end extending beyond the crossarm.

After the wire is pulled out on the ground in 0.7-mile lengths, it is placed on the extension crossarm by laying-up sticks made of long cane poles with hooks on one end. Should the poles be high, linemen climb the poles and raise the wires to the sidearms with hand lines. The final sag and adjustment is then made by pulling at short intervals, this being necessary because the numerous transpositions in the circuits are effected on six-inch point side brackets and the line wire will not adjust itself around these brackets without some assistance. Each of these brackets has four pins for making the transpositions and is so arranged that one pin of each pair is higher than its mate; this is done in order that the left-hand wire (going in direction with the line) can pass over and change positions with the wire on the right, and at the same time afford clearance between the two conductors.

The spans on these leads averaged about 130 feet, and sag requirements on this length of span at 70 degrees Fahrenheit is $9^1/_2$ inches. Seventy degrees Fahrenheit is being quoted as that was about the average temperature prevailing at the time wire was placed.

Ordinarily, where wire is being sagged and the regular one point drop brackets are used, lengths of approximately one mile can be pulled on straight runs, but, due to the slack required at the numerous 6-inch point side brackets, shorter pulls had to be made, and a rather novel arrangement was worked out.

A groundman stationed at the sagging blocks was equipped with a 62-*A* test set. The tying crew consisted of four linemen, two of whom where also equipped with small test sets attached to one of the new circuits. The men with test sets worked on transposition points at all times, and the others on intermediate poles. When the proper sag was obtained through telephone conversation with groundman at the blocks, all four men completed their ties and each proceeded four poles forward and repeated the operation.

It might be of interest to know that in placing the four circuits of wire between Dallas and San Antonio and between Dallas and Houston, a distance of 555 pole miles, the following material was involved:

1,173,760 pounds of 128 copper wire amounting to 2,240 circuit miles

33,600 six-inch side point transposition brackets

20,640 H-beam pole-extension fixtures

22,880 eight-pin six-inch-spaced crossarms

266,400 Pyrex insulators

A total of 82,400 manhours, or 10,300 mandays of labor were required in placing the wire, extension fixtures, crossarms, and other associated materials. An oddity, in connection with material used on this project, is that approximately two miles more wire was required due to the use of point-side transpositions than would normally be required on ordinary brackets.

R. P. Brown (Southwestern Bell Telephone Company, Dallas, Tex.): The paper, "Some Applications of the Type *J* Carrier System," presented by Messrs. Starbird and Mathis brings out that the first three commercial installations of these systems were made in Texas. This is of considerable significance and brings about a thought as to what condition existed in Texas that would necessitate three separate installations of these systems so soon after the development and all about the same time. It is about this condition that my discussion will be confined.

In the national plan for handling intercity toll communications, there have been designated eight regional centers so located as to serve certain areas and each connected to all the others with direct lines so as to minimize the number of switches and furnish the highest possible grade of toll service. In the Southwestern area one of the regional centers is located at Dallas, and for handling the toll traffic in the area and connecting Dallas with the other regional centers there are seven major trunk routes through Texas which are described as follows:

(*a*). Dallas-Houston
(*b*). Dallas-San Antonio
(*c*). Dallas-Longview
(*d*). Houston-Beaumont
(*e*). Cisco-West
(*f*). Dallas-Red River (Oklahoma)
(*g*). Dallas-Cisco

The latter two mentioned which required relief before the conception of the 12-channel open-wire carrier were cared for by the placing of underground toll cable during 1930.

At that time the question of relief on the other trunk routes was becoming pressing and relief by placing cable seemed to be the proper method. Studies were prepared on that basis and as the relief was first necessary in the sections to Houston and San Antonio an inverted Y layout with the base at Dallas, the fork near Waco, and the two branches extending to Houston and San Antonio appeared to be the plan to follow. The Longview and Beaumont cables would follow later as would the extension of the Dallas-Cisco cable beyond Cisco.

To care for the growth until the cable relief was forced the open-wire pole lines were being developed to the maximum circuit possibilities by the use of the single- and the three-channel open-wire telephone carrier systems. Under this expansion the physical wire had grown to about five crossarms and by using the various instrumentalities the telephone circuit possibilities had been expanded to approximately 70 circuits.

With the information that a new carrier system was being developed that could be superposed on some of the present wire without sacrificing any of the present telephone circuits, thereby providing 100 or more additional telephone circuits, and which could be grown in groups of 12 telephone channels at a time without an extreme expenditure for the initial installation, it was apparent that this new development was just the thing required here in Texas to care for our problem and the first question was just how quickly could it be made available. As a result, the plans for relief by the placing of toll cables were laid aside and new plans developed on the basis of using the equipment that was under development and which would be rushed to completion due to the immediate requirement.

To use the newly developed 12-channel system on present open-wire leads it would be necessary to do some wire respacing and retransposing before the second system is installed; however, with the initial installation sufficient circuits would be available to

care for growth and still allow present wire to be taken out of service for the rearrangements required to make the future additions. Where the circuit requirements could be cared for until the new carrier became available, as in the case of the Dallas-Longview line, the first installation would be placed on present wire and then the rearrangement work carried out, but in the cases where additional circuits had to be made available immediately, wire properly spaced and transposed for the new carrier system was placed and growth cared for using present available equipment until the new equipment was ready.

With the placing of the 12-channel systems in service for the longer distances it becomes possible to relocate the present terminals of the one- and three-channel systems to intermediate points in these trunk routes as well as in other sections, creating a very flexible circuit arrangement.

In conclusion it appears that the development of the new 12-channel open-wire carrier system was completed just in time to fit into the toll-line plan for Texas and definitely postpone for a considerable time the placing of toll cables over new routes.

R. B. Webb (Southwestern Bell Telephone Company, Dallas, Tex.): The development of a new toll instrumentality such as the type *J* carrier system immediately raises the question as to where this type of carrier can be most advantageously applied. Each major open-wire toll line must be studied to determine if the expected growth can be cared for most economically by providing additional physical circuits, additional three-channel and single-channel carrier systems, the use of the type *J* carrier systems, or by different combinations of all of these methods of relief.

The first step involved in the preparation of a toll line study is to secure a large amount of basic data, the chief elements of which are as follows:

1. Estimate of circuit requirements on the toll line, by circuit groups for several years into the future.
2. The present circuit arrangement and the number and types of carrier systems already in use on the line.
3. Basic toll-line data, covering the age of the line, pole heights, number of crossarms, and the maximum number of crossarms that can be placed.
4. Toll-entrance-cable data, listing the length, gauge, and number of conductors in the toll entrance cable at each central office involved.
5. The number and location of present carrier repeater stations.
6. The number and approximate location of repeater stations required for *J* carrier.

The most feasible plans of caring for the growth are developed from an analysis of the requirements and the local situation. The period of the study is determined by local conditions, although in general it is desirable to carry the study to a date after which the facilities will be common under all plans. Circuit layouts are made for each year considered, and all circuit additions and rearrangements, and the means used to effect them, are indicated by appropriate codes. These circuit layouts give, by successive steps, the complete program of caring for the growth under each plan during the study period.

The long-distance network is so complex that it is frequently necessary to include on the circuit layouts much more of the toll plant than the particular toll line under consideration. For instance, in the Dallas-Longview study, it was necessary to include the Longview-Shreveport line, the Longview-Jacksonville-Tyler line, and the Tyler-Mineola line. In so far as possible, the same number of toll circuits is cared for under each plan, and, if this is not done, the difference in facilities provided must be considered in interpreting the financial results of the study.

After the circuit layouts are developed, the additional outside plant construction, central-office equipment, and other items can be priced, and the total capital requirements obtained. Engineering cost studies are then made to determine the annual charges and operating expenses for each plan. The financial results thus obtained will usually indicate the proper method of toll-line relief.

The general study methods just outlined were employed in 1936 to determine the proper relief program on the Dallas-San Antonio, Dallas-Houston, and Dallas-Longview toll lines. Because each of these toll lines was approaching its maximum fill and because of the circuit growth expected, it was evident that the initial *J* carrier installation should be made on each of these lines either in 1938 or 1939. As the three toll-line problems were very similar it will only be necessary to comment on the Dallas-San Antonio study.

In this study, it was only necessary to determine what the penalties would be for deferring the *J* carrier from 1938 until 1939. It developed that thus deferring the *J* carrier relief would have required the stringing of so much toll wire and the placing of so many three-channel carrier systems that it was obviously an uneconomical procedure, and the installation was therefore carried out in 1938.

In this particular case, the study solution to the problem appeared fairly obvious from the start, since the circuit growth was about 12 circuits a year. However, in the case of a toll line not so heavily loaded, and where the annual growth is not so rapid, the economical time to install the initial *J* carrier will not be so apparent. For instance, in the study on the Houston-Beaumont toll line, the circuit growth was only about six circuits a year. The two plans of relief considered were: first, to install an initial *J* carrier system in 1939 and additional *J* systems as required; second, to provide other facilities in 1939 and 1940 and install the initial *J* carrier in 1941. This study was carried out over a ten-year period in order to make the two plans as nearly comparable as possible. It was found that the *J* carrier system should be installed in 1939 and this system will be placed in service in May of this year. Studies are to be made on other open-wire toll lines to determine when and where the type *J* carrier should be used.

L. C. Starbird and J. D. Mathis: The comments by Messrs. Keathley, Webb, and Brown are of interest in that they present additional information about the problems encountered in the application of these systems, together with amplification of some of the items mentioned in the paper. Mr. Lipscomb's comments leave several unanswered questions.

In regard to the use of nonloaded paper-insulated pairs for the *J* frequencies versus loaded pairs for the *C* frequencies, the normal attenuation of the nonloaded paper-insulated pairs is unfortunately very high. However, because of the close spacing of load coils required to load these conductors at *J* frequencies it is generally more economical to provide sufficient amplification to override the loss than it is to provide the loading. Impedance-matching transformers are provided at the filter hut and in the central office to minimize the loss to the *J* system and the crosstalk between systems that would be caused by an impedance irregularity. Nonloaded cable pairs may also be used for the *C* frequencies; however, in the application discussed, loaded pairs were already available for the *C* systems, and it was more economical to use these than to provide the amplification and impedance matching required if nonloaded pairs were used.

In regard to possible measures of reducing cross talk caused by paralleling wire near a *J* carrier repeater station, except for the more severe cases, this cross talk will be chiefly caused by longitudinal current which may be sufficiently suppressed by the insertion of longitudinal choke coils only. These coils have practically no effect on metallic currents and would not interfere with the operation of a carrier system over the paralleling wire at frequencies up to about 150 kilocycles. In the more severe cases of parallel it would be necessary to insert cross-talk suppression filters in the paralleling wire. The loss caused by these filters in the pass band is from about 0.1 decibel at voice frequency to 0.7 decibel at 30 kilocycles for each point where applied. In case the paralleling wire is to be used for carrier current at frequencies above 30 kilocycles, it will be necessary to co-ordinate repeater-station locations in order to prevent level differences. By co-ordinating repeater locations, the filters required on each line because of the systems on that line would serve to minimize the interaction cross talk without other special measures.

The questions are discussed in more detail in the papers listed as references and in the discussion with reference to those papers.

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☐ 39-80	Ilgenfritz, Hunter & Whitman	Line Problems in the 12-Channel Open-Wire Carrier System
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☐ 39-117	Pipes	Transformation Theory of General Static Polyphase Networks
☐ 39-118	Stansbury & Jochem	Capacitor Relay Timing in Industrial Control
☐ 39-119	Markwart	Mingled Hydro- and Steam-Power Production in California
☐ 39-120	St. Clair	Multiwinding Transformers with Synchronous Condensers
☐ 39-121	Marti & Taylor	Wave Shape of 30- and 60-Phase Rectifier Groups
☐ 39-122	Lewis & Foust	Lightning Investigation on Transmission Lines
☐ 39-123	Alford & Pickles	Radio-Frequency High-Voltage Phenomena
☐ 39-124	Goldberg	A High Gain D-C Amplifier for Bioelectric Recording
☐ 39-125	Hunt	Some Practical Measuring Devices
☐ 39-126	Warren	A New Time Standard
☐ 39-127	Benson & Strang	12-Kv Metal-Enclosed Bus and Switch Structure
☐ 39-129	Halperin	Testing of Distribution Arresters
☐ 39-130	Harder, Langguth & Woods	Transient and Steady-State Performance of Potential Devices
☐ 39-131	Hunt & Vivian	Sensitive Ground Protection for Radial Distribution Feeders
☐ 39-132	Skeats & Saylor	High-Capacity Hydro-Blast Circuit Breaker
☐ 39-133	Boice, Crary, Kron & Thompson	The Direct-Acting Generator Voltage Regulator
☐ 39-134	Malti & Herzog	Fractional-Slot and Dead-Coil Windings
☐ 39-135	AIEE Committee Report	Report of Progress of the Art in Electrical Machinery
☐ 39-136	Dickerson & Mahan	Painting the Golden Gate International Exposition With Light
☐ 39-137	Kiltie	New Type of D-C to A-C Vibrator Inverter
☐ 39-138	Davis	Signal System, San Francisco-Oakland Bay Bridge Railway
☐ 39-139	Hanna & Tritle	Electrical Equipment of the Steam-Electric Locomotive
☐ 39-140	Reinitz & Wiseman	A New Technique for Lead Cable Sheathing
☐ 39-141	Smith & Tenney	Temperature Survey of the United States
☐ 39-142	Lamberger	Variable-Voltage Equipment for Rotary Drilling Rigs
☐ 39-143	Weeks, Giesecke & Lathrop	Electrical Engineering and the Petroleum Refiner
☐ 39-144	Aggers, Foster & Young	Instruments and Methods of Measuring Radio Noise
☐ 39-146	d'Humy	Recent Developments in Telegraph Switching

Minneapolis Meeting

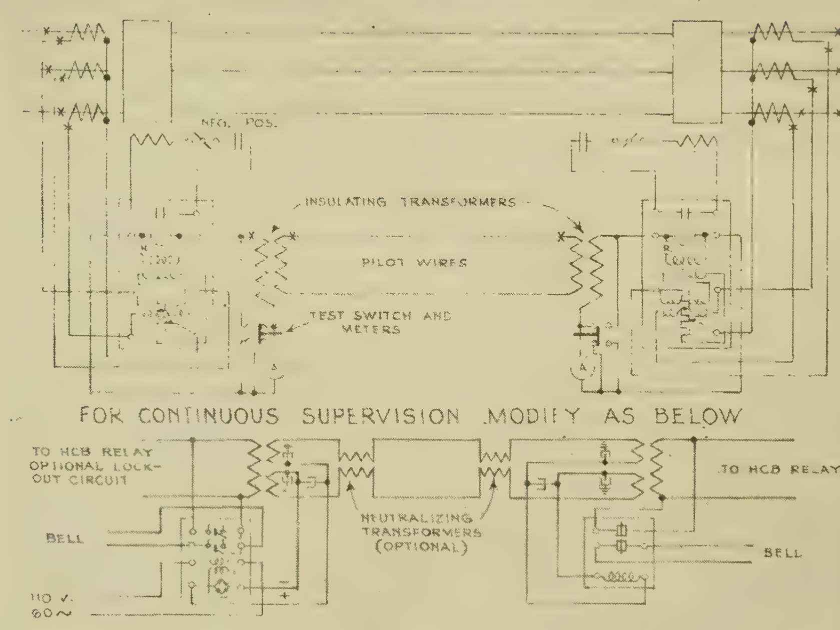
☐ 39-147	LeClair	Arc-Furnace Loads on Long Transmission Lines
☐ 39-148	Summers	An Unstable Non-Linear Circuit
☐ 39-149	Foust & Robats	Developments in Impulse-Voltage Testing
☐ 39-150	Peterson, Rudge, Monteith & Ludwig	Protector Tubes for Power Systems
☐ 39-151	Fountain, Dissmeyer & Elzi	Co-ordination of Control of Synchronous-Machine Excitation
☐ 39-152	Maxfield, Hegbar & Eaton	Importance of Gas in Electrodes for Glow-to-Arc Transition
☐ 39-153	Swanson	Polarized Field Frequency Control of Synchronous Motors
☐ 39-154	Smith & Bostwick	Ratio Differential Protection of Transmission Lines
☐ 39-155	Hoard	Transit-System Modernization, Choice of Vehicle
☐ 39-156	Bryant & Newman	Electronic Measurement of Surge-Crest Voltages
☐ 39-157	Bryant & Newman	Measurement of Very Short Time Lags
☐ 39-158	Butler	Factors Influencing Shunt Capacitor Equipments
☐ 39-159	Holbrook & Dixon	Load-Rating Theory for Multi-Channel Amplifiers
☐ 39-160	Kurtz	Dynamic Characteristics, Single-Phase Induction Motor
☐ 39-161	Parsons	Vaults for A-C Secondary Networks
☐ 39-162	Stephens & Gehrenbeck	Corrosion of Fine Copper Wires Carrying a Potential
☐ 39-163	Willey	Emergency Radio as Safeguard to Telephone Service

Scranton Meeting

☐ 39-164	Renshaw	Mercury-Arc Rectifiers in Coal Mining
☐ 39-165	Bell	Lightning Investigation on a 220-Kv System, III
☐ 39-166	Caldwell	Electrical Apparatus in the Steel Industry
☐ 39-167	Armstrong	NEMA Resistors for Motor Starting and Speed Control
☐ 39-168	Penney	Temperature Rise of Electrical Apparatus
☐ 39-169	Compton	Class B Insulation for Auxiliary-Type D-C Motors
☐ 39-170	Edmunds	Adjustable-Speed A-C Motors
☐ 39-171	Beavers	Selection of Distribution Transformer Size
☐ 39-172	Hentz & Thielman	Power Supply for Suburban Areas
☐ 39-173	Camilli	Current Transformer Design

*TYPHONITE ELDORADO PENCIL PAGE

Ratio-Differential Pilot Wire Protective Relay Circuits

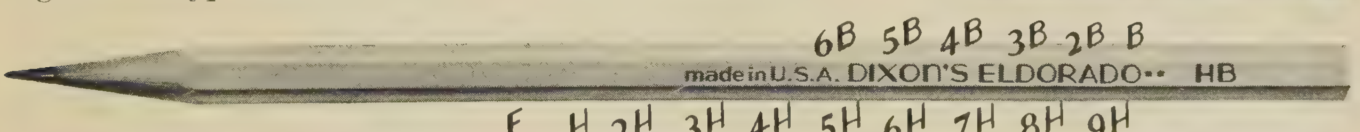


This Typhonite Eldorado drawing shows the application of a new relay for "ratio-differential pilot wire protection" of an electric power transmission circuit. For a circuit of this nature, the idea of comparing voltages, currents or power from both ends of a transmission line section through the medium of pilot wires, or indirectly by use of carrier-current signals, has long been used as a means of detecting transmission line faults.

This relay and circuit accomplish desirable simplification in both circuit and equipment, with consequent improvement in character and dependability of operation. The specific relay involved in the circuit has been developed by Westinghouse and is known as "type HCB."

FREE: A blueprint of this circuit made directly from the original drawing will be sent upon request. Just write to Pencil Sales Department, JOSEPH DIXON CRUCIBLE COMPANY, Jersey City, N. J. for blueprint No. 42-J12. And note the ideal blueprint resulting from Typhonite Eldorado's clean, opaque lines.

* **TYPHONITE** is a new form of natural graphite, used exclusively by Dixon in making leads for Eldorado Pencils. Typhonite consists of extremely minute particles produced by a whirlwind or typhoon of dry steam. This new exclusive Dixon process is one of the reasons why Eldorado pencils hold their points longer, give off freely, and make such opaque lines and figures.

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Industrial Notes

New Western Electric Subsidiary.—To meet the requirements of broadcasting stations, airlines, the government services and other similar users of its communication equipment outside of the Bell System, the Western Electric Company has formed a new branch to be known as the Specialty Products Division. This unit will be responsible for such by-products of telephone research as hearing aids; aviation, marine and police radio; broadcasting equipment; sound systems and equipment made to specification for the United States government. The new division, which began formal operation last month, is located at the Kearny, N. J., works of Western Electric, with F. R. Lack as manager. Mr. Lack has been associated with the Bell System since 1911.

New Division for Copperweld Steel.—Frederick J. Griffiths has been appointed executive vice-president in charge of the newly created alloy steel division of the Copperweld Steel Company. The appointment follows the earlier announcement by S. E. Bramer, president, that the Copperweld Steel Company had purchased the plant and assets formerly owned by the American Puddled Iron Co., Warren, Ohio. On the 423-acre property a $2,000,000 steel plant will be erected, including electric furnaces and other equipment capable of producing 100,000 tons of high quality alloy steel annually, part of which is to be used in the manufacture of Copperweld wire, rods, and strands of copper-covered steel. The present buildings of the American Puddled Iron Company will furnish only a nucleus for the new plant which, when completed, will spread over 285,000 square feet of floor space. The finishing operations now being carried on in the Copperweld Steel Company's plant at Glassport, Pa., will continue as usual.

Expands Engineering Department.—A new engineering building is under construction for Walter Kidde & Co., makers of carbon dioxide fire extinguishing equipment, widely used in the electrical field, on the company's manufacturing site at Bloomfield, N. J. The new structure, to be devoted exclusively to engineering and research work, will house a large staff of engineers when completed. Commenting on the announcement of the new addition, President Walter Kidde cited the branching of the company into new fields, which calls for enlarged engineering facilities.

Roller-Smith Appointments.—New sales agencies recently appointed by the Roller-Smith Company, Bethlehem, Pa., include J. J. Thalheimer, 242 Fountain Ave., Dayton, for southern Ohio, southern Indiana and Kentucky; Walter V. Gearhart Co., Volunteer Bldg., Atlanta, for the states of Georgia, North and South Carolina and parts of Alabama and Florida; and the Public Service Supply Co., Inc., Midland Bldg., Cleveland, to cover the utilities in northern and southeastern Ohio.

Roebling Moves New York Office.—The John A. Roebling's Sons Co., Trenton, N. J., manufacturers of wire and wire rope, insulated wire and cables, and wire cloth and netting, has moved its New York City offices from 107 Liberty St., to the 20th floor of 19 Rector Street.

Cutler-Hammer Appointments. — Terry Fisher has been appointed to take charge of the northern Indiana territory for Cutler-Hammer, Inc., with headquarters at South Bend. Clayton P. Innes is now representative in the company's Omaha territory.

Larger Quarters for Littelfuse.—For the fifth time in 12 years, Littelfuse, Inc. has been obliged to enlarge its manufacturing facilities, increase its plant capacity and move to enlarged quarters. The new plant address is 4757 Ravenswood Ave., Chicago.

Trade Literature

Relays.—Catalog, 32 pp. Describes relays, timing devices, and thermostats; prices are included. Struthers Dunn, Inc., 1315 Cherry St., Philadelphia, Pa.

Synchronous Motors.—Bulletin GEA 1191B, 52 pp. Describes in detail the characteristics, advantages and applications of synchronous motors; profusely illustrated. General Electric Co., Schenectady, N. Y.

Megohm Bridge.—Bulletin 459, 4 pp. Describes a new, type 544B, megohm bridge and its 500-volt power supply, for insulation resistance measurements. General Radio Co., Cambridge, Mass.

V-Belt Drives and Motors.—Bulletin B 6029, 28 pp. Includes comprehensive data in ready reference form for estimating costs, types and sizes of "Texrope" drive equipment and motors to be used under various operating conditions. Allis-Chalmers Mfg. Co., Milwaukee, Wis.

Lightning Arresters.—Bulletin GEA 1304F, 20 pp. Describes "Thyrite" station-type, lightning arresters applicable either indoors or outdoors for the protection of large or small generating or substation equipment. General Electric Co., Schenectady, N. Y.

Radium Detector.—Bulletin "E," 4 pp. Describes a Geiger counter type radium detector for discovering lost radium and for prospecting for radioactive materials, investigations in radioactivity, etc. Geophysical Instrument Co., 1315 Half St., Washington, D. C.

Switch and Bus Insulators.—Catalog, 18 pp. Describes a new line of bus and switch insulators assembled with an improved asphaltic compound known as "Permalastic" which, according to the manufacturer, tests have shown adds many years to insulator life. Complete data is given on all standard NEMA and several specially designed switch and bus insulators. Conductor clamps, adapters and spacers are also described. Porcelain Products, Inc., Parkersburg, W. Va.

Porcelain.—Bulletin, 12 pp. "A Survey of Technical Characteristics of Molded Ceramic Products" for wiring, heating and special purposes. Details the physical properties and applications of specific ceramic formulas. The Star Porcelain Co., 11 Muirhead Ave., Trenton, N. J.

Lathes.—Catalog 100, 112 pp. Said to be the most complete lathe catalog ever published; contains over 250 illustrations and shows 75 different sizes and types of back-geared, screw cutting lathes for production manufacturing, tool room and general shop work. South Bend Lathe Works, South Bend, Ind.

Underground Multiple Connectors.—Bulletin, 4 pp. Largely an illustrative treatment of the application of Burndy "Moles," (multiple tap connectors) available in many types, for underground secondary distribution systems. Features include elimination of soldering and junction boxes; cables with different types of insulation may be joined; installations can be made in limited space and in much less time than required for soldered, multiple splices; different cable sizes are accommodated in each outlet and provision made for rapid future connections. Burndy Engineering Co., Inc., 459 E. 133d St., New York City.

Varnished Cambric Cable Specifications

The Insulated Power Cable Engineers Association has recently issued a Fourth Edition of its "Specifications for Varnished Cambric Insulated Cables." This revised edition contains many changes which improve the accuracy and extend the scope of several sections and tables, bring certain requirements into conformity with the latest editions of correlated existing specifications, and insure the furnishing of cable embodying improvements in materials and manufacturing technique which have been made since the third edition was issued in 1934.

Probably the most important of these changes is the increase, ranging from 8° to 15°C. in the permissible maximum cable operating temperature over that previously permitted.

The Association calls attention to an important change in the title of Table VII, on page 8 of the Fourth Edition, which should read—"Recommended Thickness of Insulation—Single Conductor Cable and Multiple Conductor Shielded Cable."

Copies of the Specifications (28 pp.) are available without charge upon request to the Insulated Power Cable Engineers Association, 420 Lexington Ave., New York City.

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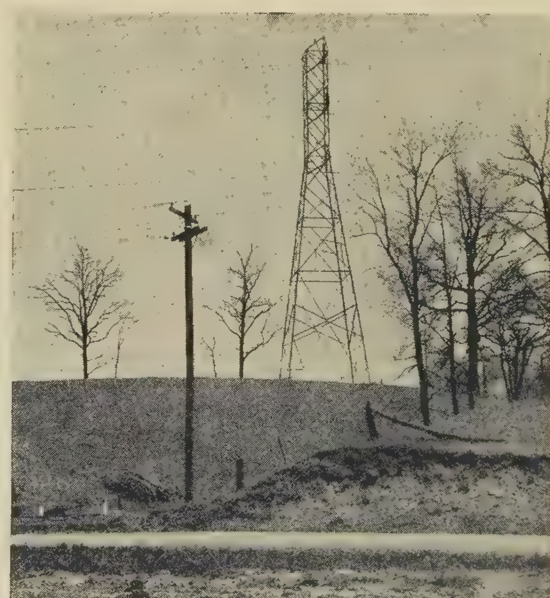
That's exactly how experienced operators feel about A.C.S.R. On lines built with these conductors, they find that maintenance is a minor item on their cost sheets. 700,000 miles of A.C.S.R., some of it in service over a quarter of a century, have proved its dependability.

A.C.S.R. conductors have high strength, long life and ample conductivity. Spans can be long and first costs low, without sacrificing quality. Let us assist you in engineering your next line; we'll supply you with data on any type of construction. Aluminum Company of America, 2149 Gulf Building, Pittsburgh, Pennsylvania.



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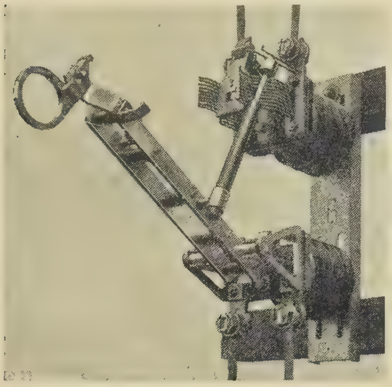
Aluminum Cable Steel Reinforced

FOR RURAL LINES AND POWER TRANSMISSION

New Products

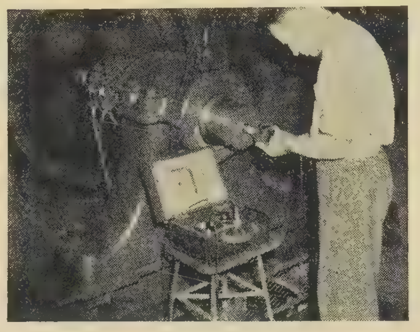
Disconnecting Switch.—A recent development of the James R. Kearney Corp., St. Louis, Mo., this by-pass disconnecting



switch may be used for isolating current transformers and voltage regulators without interrupting main line service. The main blade is composed of two separate blades which are insulated from each other and are respectively and electrically in series with the primary leads of the transformer or regulator. When the main blade is pulled open to isolate the transformer or regulator, a short circuiting blade automatically closes the main circuit through the line-side terminals of the switch. Mounted on the right side of the main blade is an auxiliary switch which is shown interrupting the exciting current.

Mercury Switch.—An improved metal mercury switch and its new double reduction (two reducing agents) which prevents deterioration of the mercury, eliminates possibility of contact interruptions, increases efficiency and reliability in operation, according to the manufacturer, Durakool, Inc., Elkhart, Ind. It is reported to be the only commercial mercury switch available with over 40-ampere capacity, and up to 200 amperes (larger on special order), and is said even to withstand temporary short circuits, and to operate on small tilt with remarkably little energy. The improved switch allows many new applications from wall switches to motor starters and is now in use on electrical devices requiring from a few operations a month to 2,400 a minute.

Portable Dynamic Balancing Equipment.—The General Electric Company offers a portable dynamic balancing equipment for use wherever rotating masses require balancing to eliminate vibration. The new device is a self-contained precision instrument, capable of measuring the amount and phase angle of unbalance vibration present in the bearing pedestals of a rotating machine running in its own or substitute bearings, at any speed between about 600 to 5,000 rpm. Being portable, it permits the balancing of rotating equipment without the removal of the rotor from the machine and the balancing of rotors that are too heavy for previously available portable balancing machines. Because the balance is made under operating conditions, this

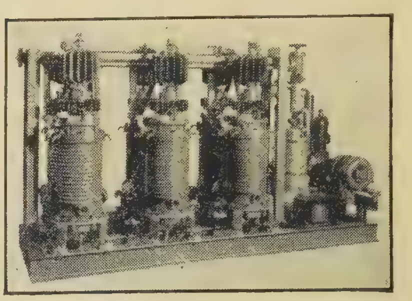


equipment improves the quality of the result, as the changes effected by load and by the foundations are included. It has also been found a time-saver in balancing auxiliary equipment in control stations and manufacturing plants.

Illuminated Magnifier.—Developed originally for examining the inside portion of a valve and valve seat, the "Pike-O-Scope," an illuminated magnifier, also can be used for inspection of many other types of machinery.

A set of achromatic triple lenses are lighted by a special lamp built into the bottom of a cylinder which also serves as a handle. It may be attached to any electric outlet or used with dry cell batteries. The lamp cylinder and the threaded ring for the lenses are made of a heavy, shock-resistant, Bakelite molded material. It is said that defects can readily be seen at a distance of two feet from the eye.

Single-Anode Power Rectifier.—A new single anode power rectifier developed by the Allis-Chalmers Manufacturing Co., Milwaukee, Wis., is designated as the "Excitron" type to distinguish it from the



multi-anode type also manufactured by the company. Instead of having all the anodes in one vacuum chamber, the Excitron consists of factory-assembled groups of relatively small vacuum tanks, each containing a single anode and its individual cathode. The tanks, which are externally cooled by water, are generally mounted in groups of six on a heavy structural steel frame. This frame also supports the vacuum pumping equipment, which is connected to the tanks through a vacuum pipe manifold. The arc ignition and control equipment, too, is mounted and wired on this frame. The single-anode Excitron unit is particularly suited for applications where the amount of available head room is limited. Due to the closer spacing between an anode and its associated cathode than in the multi-anode type of rectifier, the voltage drop in the mercury arc is reduced and, as a result, the company claims that the efficiency of the Excitron unit is from 3 to 4 per cent higher in the lower voltage brackets. Although this advantage is particularly pronounced at 250–300 volts, the Excitron rectifier is nonetheless equally adaptable for use at higher voltages.

Small Diameter Switchboard Wire.—To permit fast, neat installations, a new "Deltabeston" switchboard wire insulated with purified asbestos and plasticized polyvinyl chloride compound of the type used in "Flamenol" wire, has been announced by the General Electric appliance and merchandise department, Bridgeport, Conn. The reduction in overall diameter size of the new wire is particularly marked, with No. 12 wire, for one example, having been reduced from the standard size of .225 inches to .195 inches. Reductions for other wire sizes are proportionate. The new G-E Deltabeston switchboard wire is extremely flexible and very light. Insulation will not break or crack at bends. A paper separator between the copper and the insulating wall permits clean, quick stripping. The wire is approved by Underwriters Laboratories for temperatures up to and including 90 degrees Centigrade.

A complete list of Standards of the AIEE appears on the back cover of the 1939 Yearly Index, bound separately and accompanying this issue.

Maybe You Need a *Specially Designed*

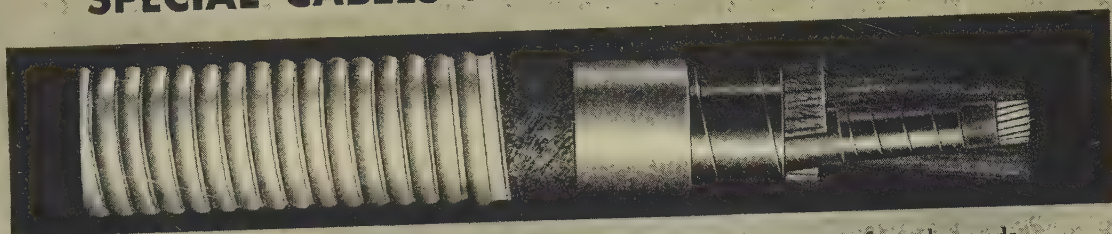
DELTABESTON CABLE

The standard line of Deltabeston Wires and Cables will answer a lot of your wiring problems. There's a wide range of types and sizes—from Magnet Wire to Power Cable—durably protected with heat-resisting insulations.

But—that's just part of the story!

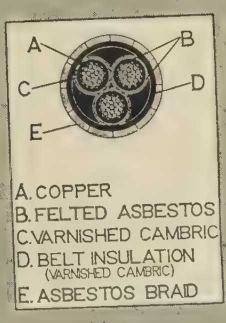
Deltabeston Engineers can help you fill out-of-the-ordinary requirements, too. Years of experience and a knowledge of modern insulating materials enable these men to suggest specially designed cables to fit your most unusual applications. Here's further proof that: *"Where heat is a problem, there's a Deltabeston answer."*

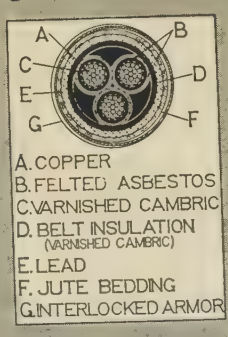
SPECIAL CABLES FOR SPECIFIC JOBS

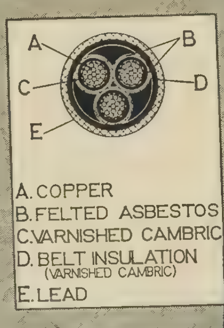


This cable, built for a particular central station application, answers four demands.

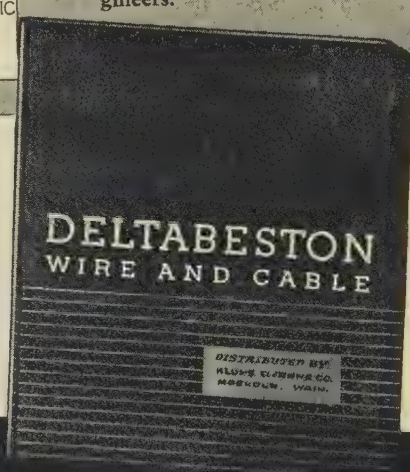
1. It permits high current densities necessary during peak load periods.
2. It resists extreme ambient temperatures.
3. Insulation is flame-proof—isn't damaged by severe treatment.
4. Interlocking armor eliminates need for rigid conduit installation.







These cross-section views illustrate the construction of three specially designed Deltabeston Cables. Each one was engineered and built to fit a specific job. Each one incorporates the knowledge and study of able Deltabeston Engineers.



Next time a cable problem is troubling you, call a Deltabeston Engineer. He'll be glad to help you—without cost or obligation. Right now, send for the brand new Deltabeston Catalog—crammed with helpful information. Address Section Y-92512, Appliance and Merchandise Department, General Electric Company, Bridgeport, Connecticut.

Deltabeston Wires and Cables are distributed by General Electric Merchandise Distributors and Graybar Electric Company.

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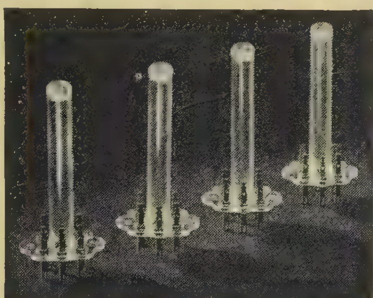
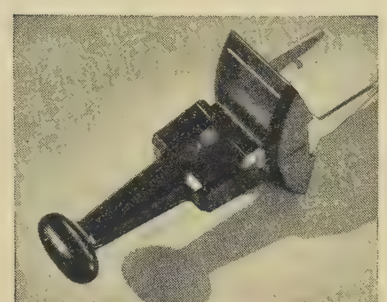
For Unusual Insulation Demands

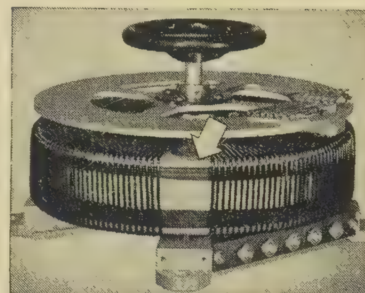
consider Versatile Bakelite Plastics



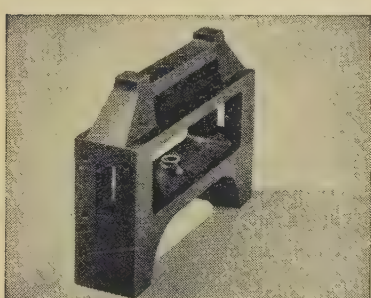
Bakelite Polystyrene **has exceptionally low power factor necessary for ultra-high frequency equipment such as these Transparent R.C.A. Television Coil Forms.**



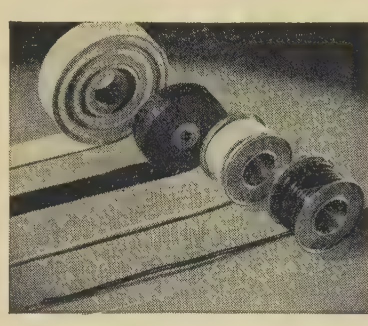
Impact Bakelite Molded **imparts toughness and highly protective electrical insulation to constantly-handled devices like this I.T.E. Circuit Breaker Handle.**



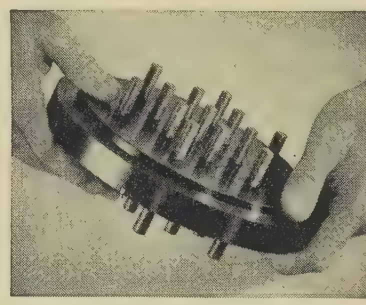
Bakelite Laminated **is rugged and enduring. It is used here for 12" Winding Forms that withstand winding tensions of 600-700 lbs. and temperatures up to 200°F.**



Low-Loss Bakelite Molded **minimizes power losses in high frequency instruments such as this Leeds & Northrup Capacitor Frame. It is resistant to moisture and chemicals.**



Vinyl Resin Compounds **for extruded wire and cable coatings and for insulating tapes, possess superior electrical and chemical resistance and aging characteristics.**



Bakelite Molded **Allis-Chalmers Transformer Terminal Blocks, that resist oil pressures of 30 lbs. per sq. in., are molded in one operation with pre-positioned metal studs.**

THERE ARE numerous high-dielectric Bakelite Materials at your disposal to help you solve efficiently insulation requirements in the generation, transmission or use of electric power.

Many of these versatile Bakelite Plastics are especially resistant to heat, moisture, oil, chemicals and mechanical stress. All effectively contribute greater durability and higher operating efficiency to a diversity of electrical equipment and devices.

The applications illustrated show only six types of Bakelite Plastics and just one use of each. It will pay you to learn more about the great variety of these materials, how they are meeting many unusual insulation demands, and how they can solve your own insulation problems. Write for Portfolio 33, of illustrated booklets.

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Engineering Societies Employment Service

MAINTAINED for their members by the national societies of civil, mining, mechanical, and electrical engineers, in co-operation with other organizations. An inquiry to any of the three offices will bring full information.

A weekly bulletin of engineering positions open is available to members of the co-operating societies at a subscription of $3 per quarter or $10 per annum, payable in advance.

In the interest of effective service, it is essential that members using the employment service keep the bureau office serving them advised at reasonable intervals concerning their availability for employment, concerning any change in status, and immediately upon acceptance of any employment.

Employers interested in the following announcements should address replies to the key numbers indicated, and mail to the New York office.

Position Open

ELECTRICAL DESIGNERS, ages not over 45, graduate engineers with at least three years' experience in the design of light and power equipment. Salary, $3250–$4000 a year, plus transportation expenses. Y-4848-B.

Men Available

E.E., Carnegie Tech 1932, REG ENGR W.VA.; 5 yrs elec problems coal mines (purchase to consumption) with consulting engg firm. Knowledge higher accountancy. Exper highway surveying. Now studying Spanish. E-550.

E.E., M.S., Yale, 33, married; chief engr or head des or devpmt divisions. Now employed in exec pos. Exper and capable research, devpmt, mfr of elec, mech, electrochem products. E-551.

B.S.E.E., 1939, 21, single; desires work as jr engr with mfg co allowing nights for continued study in cost acctg and prod. Location immaterial, salary secondary. E-552.

PLANT ENGR, EE 1922, 16 yrs indus plant constr, oprn, des, devpmt. NJ Grade A oprtg license; now employed; desires application or constr pos with increased opportunities; foreign language, German. E-553.

B.S.E.E., Alabama 1939, single, 23, desires pos as jr engr with advancement. Location immaterial, salary secondary. E-554.

B.E.E., O.S.U., '35, single, 29, now employed; 4 yrs conveyor engg draftg, des. Devpmt work desired. Pleasing personality, good organizer. Middlewest preferred. E-555.

ELEC ENGR, 26, single; B.S. Washington State Col, '39; 3 mos Rural Electrification Administration (now employed). Shop work. Available immediately. E-556-3910-C-4-San Francisco.

RECENT GRAD, B.S.E.E. Union '39, good grades, some surveying exper, desires work as jr engr with a mfg co or util. E-557.

B.E.E., Catholic Univ '37, 22, single; 2 yrs exper in elec constr. Desires pos in constr work. Location immaterial. E-558.

B.S.E.E., 1939, single, 22. Specialized pwr and radio engg; 18 mos co-operative work. Tau Beta Pi, Eta Kappa Nu. Desires pos as jr engr with advancement; location immaterial, salary secondary. E-559.

B.S.E.E., Bucknell, 1935, grad work; now teaching (3 yrs) elec constr and math in vocational high sch. Exper radio, sound, projection eqpt, testg. Interested in des, radio, electronics, sound, instruction. E-560.

GRAD E.E., Swiss Fed Inst Tech, 1937, single, 26; one yr pub util USA, 1½ yrs shop exper as student engr; summer exper on test floor; speaks German, French fluently. E-561.

E.E. sound and projection engr, 28. Desires pos in electronic field, preferably in the East. Familiar with motion picture sound work and theatre constr; also 8 yrs draftg exper. E-562.

GRAD E.E., married; 12 yrs exper elec constr, gen wiring, theatre, church and indus ltg. Some draftg exper. Has energy and administrative ability. Desires pos with engg future. E-563.

ELECTRICIAN, 38, married; 18 yrs maintenance, oprn, constr, on pwr plants, substations, mfg plants; also 4 yrs So. Am. mining exper. Location immaterial; available immed. Salary secondary, but desires permanent pos. E-564.

ENGINEERING SOCIETIES EMPLOYMENT SERVICE
NEW YORK 31 W. 39th St. SAN FRANCISCO 57 Post St. CHICAGO 211 W. Wacker Dr.

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General Electric Co., Schenectady, N. Y.

AMMETER COMPENSATING COILS
Minerallac Electric Co., Chicago

AMMETERS, VOLTMETERS
(See INSTRUMENTS, ELECTRICAL)

BATTERY CHARGING APPARATUS
General Electric Co., Schenectady, N. Y.

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National Carbon Co., Inc., Cleveland, O.

BUS BAR SUPPORTS
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National Elec. Products Corp., Pittsburgh

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DYNAMOS
(See GENERATORS AND MOTORS)

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General Electric Co., Schenectady, N. Y.

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General Electric Co., Schenectady, N. Y
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Weston Elec. Instrument Corp., Newark, N. J.

INSULATING MATERIALS

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Moulded

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General Electric Co., Bridgeport, Conn.

Porcelain

Universal Clay Products Co., Sandusky, O.

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Okonite Company, The, Passaic, N. J.
Roebling's Sons Co., John A., Trenton, N.J.

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RESISTORS
Aerovox Corp., New Bedford, Mass.
General Electric Co., Schenectady, N. Y.
General Radio Co., Cambridge, Mass.
International Resistance Co., Philadelphia
Ohmite Mfg. Co., Chicago

RHEOSTATS, LABORATORY
General Electric Co., Schenectady, N. Y.
International Resistance Co., Philadelphia
Ohmite Mfg. Co., Chicago

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SUB-STATIONS
General Electric Co., Schenectady, N. Y.

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General Electric Co., Schenectady, N. Y.
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General Electric Co., Schenectady, N. Y.
Minerallac Electric Co., Chicago

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General Electric Co., Schenectady, N. Y.
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American Bridge Co., Pittsburgh

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Acme Elec. & Mfg. Co., Cuba, N. Y.
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General Electric Co., Schenectady, N. Y.

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American Steel & Wire Co., Cleveland, O.
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WIRES AND CABLES

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Aluminum Co. of America, Pittsburgh

Armored Cable

American Steel & Wire Co., Cleveland, O.
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General Electric Co., Schenectady, N. Y.
Kerite Ins. Wire & Cable Co., New York
National Elec. Products Corp., Pittsburgh
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Copperweld Steel Co., Glassport, Pa.
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Okonite-Callender Cable Co., Passaic, N. J.
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American Steel & Wire Co., Cleveland, O.
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Rubber Insulated

American Steel & Wire Co., Cleveland, O.
Crescent Ins. Wire & Cable Co., Trenton, N.J.
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American Steel & Wire Co., Cleveland, O.
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Weatherproof

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General Cable Corp., New York
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National Elec. Products Corp., Pittsburgh
Okonite Company, The, Passaic, N. J.
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X-RAY UNITS, INDUSTRIAL
General Electric X-ray Corp., Chicago, Ill.

INDEX TO ADVERTISERS